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Vol. 4

PROCEEDINGS OF THE U.S. NUCLEAR REGULATORY COMMISSION

Fourteenth Water Reactor Safety Information Meeting

Volume 4

- ECCS Rule Revision
- Integral Systems Testing
- Separate Effects/Experiments and Analyses
- 2D/3D Research

ORIGINAL

Held at
National Bureau of Standards
Gaithersburg, Maryland
October 27-31, 1986

U.S. Nuclear Regulatory Commission

Office of Nuclear Regulatory Research

Proceedings prepared by
Brookhaven National Laboratory



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II

ABSTRACT

This six-volume report contains 156 papers out of the 175 that were presented at the Fourteenth Water Reactor Safety Information Meeting held at the National Bureau of Standards, Gaithersburg, Maryland, during the week of October 27-31, 1986. The papers are printed in the order of their presentation in each session and describe progress and results of programs in nuclear safety research conducted in this country and abroad. Foreign participation in the meeting included thirty-four different papers presented by researchers from Canada, Czechoslovakia, Finland, Germany, Italy, Japan, Mexico, Spain, Sweden, Switzerland and the United Kingdom. The titles of the papers and the names of the authors have been updated and may differ from those that appeared in the final program of the meeting.

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PROCEEDINGS OF THE
14th WATER REACTOR SAFETY INFORMATION MEETING

October 27-31, 1986

Published in Six Volumes

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PROCEEDINGS OF THE
FOURTEENTH WATER REACTOR SAFETY INFORMATION MEETING

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REVISION OF THE ECCS RULE

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ABSTRACT

The U.S. Nuclear Regulatory Commission (NRC) staff has proposed a revision to the ECCS rule contained in 10 CFR 50.46 and Appendix K based upon a decade of research on ECCS performance. The rule change is expected to provide more realistic and technically defensible regulation and has the potential to provide significant economic benefit.

INTRODUCTION

Title 10 of the Code of Federal Regulations, Section 50.46 (10 CFR 50.46) provides "Acceptance Criteria for Emergency Core Cooling Systems (ECCS) in Light Water Nuclear Reactors." These are the regulations that specify how emergency core cooling systems are to be designed for nuclear power plants in the United States. Paragraph 50.46(a) requires that calculations of ECCS performance be performed to show that the ECCS meet certain criteria in the event of a loss-of-coolant accident (LOCA). Paragraph 50.46(b) requires that in the event of a LOCA, the ECCS must limit peak cladding temperatures (PCT) to less than 2200°F, limit total cladding oxidation to less than 17% of initial cladding thickness, limit hydrogen generation to 1% of that which would be generated if all the cladding were to oxidize, maintain the core in a coolable geometry and provide long-term cooling. Section 50.46 also requires that these calculations be performed using the methods specified in Appendix K to Part 50, "ECCS Evaluation Models."

Section 50.46 and Appendix K are applicable to all LOCAs (small, intermediate and large) and a break size study is required to determine the most limiting case. Historically, the large-break LOCA has been most closely associated with this rule. This association with large LOCA has been due to the fact that large LOCA is usually calculated to be the most severe and limiting LOCA when Appendix K calculational methods are used. Appendix K methods contain many deliberate conservatisms, particularly

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for large-break LOCA calculations, because of the limited knowledge of ECCS performance during a LOCA that was available in 1975 when Appendix K was written. It is now known that the PCT during a large LOCA would be much lower than that calculated using Appendix K methods.

In the decade since Appendix K was written, the NRC, DOE (including AEC and ERDA) and U.S. industry have spent almost a billion dollars on ECCS research. This research, combined with a sizable foreign research contribution, has significantly increased our understanding of ECCS performance during a LOCA. Several key results from this research should be highlighted.

1. This research has shown that Appendix K methods are highly conservative and that actual temperatures during a large-break LOCA would be much lower than those calculated using Appendix K methods.
2. The large-break LOCA may not always be the limiting licensing transient and small-break LOCA may be more important in terms of plant safety.
3. The research performed since the ECCS rule was written has provided sufficient knowledge so as to allow realistic calculations of ECCS performance with quantifiable uncertainty.
4. The conservatism in Appendix K is larger than necessary to cover calculational uncertainty and this excessive conservatism may be unnecessarily restricting the operation of some reactors.

Because of the research results highlighted above, the NRC staff has proposed revising the ECCS rule contained in 50.46 and Appendix k to make it consistent with our current knowledge of ECCS performance and to make regulation more realistic and technically defensible.

Proposed Rule

The NRC staff has been considering a revision to the ECCS rule since 1978 when a Notice of¹ Proposed Rulemaking was published in the Federal Register¹. This activity was significantly delayed by the activities required following the accident at Three Mile Island. The effort was again resumed in 1981 at a low level of effort. In 1983 an interim method² of evaluating ECCS performance, as discussed in SECY-83-472², was adopted to provide relief from unnecessary operating restrictions until the ECCS rule could

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be revised. This method still must conform to the legal requirements of 50.46 and Appendix K. However, when using the SECY-83-472 method, the NRC staff requires no additional conservatism in evaluation models other than that specifically required by Appendix K, subject to the following conditions. The licensee is required to perform a realistic or best estimate calculation of the LOCA and perform an evaluation of the uncertainty in that calculation at the 95% probability level. If this second calculation shows that the Appendix K calculation contains sufficient conservatism to cover uncertainty in actual ECCS performance, no further conservatism is required in the Appendix K licensing model. It should be noted that this method is an interim method to be used only until the ECCS rule can be revised. Based on favorable experience with this method, the NRC staff has proposed to revise the ECCS rule consistent with SECY-83-472.

The revised 50.46 proposed by the NRC staff would eliminate the requirement to use the methods of Appendix K and in its place require that ECCS performance be calculated using an evaluation model that "realistically describes the behavior of the reactor system during a loss-of-coolant accident" based upon "comparisons to applicable experimental data." Further, the uncertainty of the calculation must be estimated so that there is a "high level of probability that the criteria would not be exceeded." The criteria are the 2200°F PCT and other criteria of 50.56(b) which would be unchanged. This is a very general rule that would be clarified through a regulatory guide³. The regulatory guide would specifically state that the "high probability" requirement of the rule would be satisfied by a 95% probability or simply two standard deviations to eliminate confidence limit concerns. The regulatory guide would also provide general guidance on how to perform a best estimate calculation and what would be expected in the uncertainty evaluation. However, the licensee would be free to propose their own methods. A second document would be provided to summarize the large amount of research generally applicable to best estimates of ECCS performance and to describe NRC's efforts to quantify uncertainty in ECCS performance calculations⁴.

In addition to the major change described above, several other changes are included in the staff proposal. Appendix K would remain as an alternative to performing the uncertainty evaluation required with the best estimate approach. This would allow current or future licensees to continue to use existing methods. However, the Dougall-Rohsenow film boiling heat transfer correlation would be removed from Appendix K as a generally acceptable model since it is now known that this correlation may

produce nonconservative results under some conditions. The revised rule would state specific conditions for continued use of this correlation in evaluation models approved prior to implementation of the proposed rule revision.

The proposed rule revision also revises the requirements for reporting of changes or errors in approved evaluation models. The current rule contains no specific requirements in this area. Legal precedence has, however, placed very stringent requirements in this area. Currently, licensees are required to report any error or change that results in a change in PCT by more than 20°F and to take immediate steps to correct the model. This has often required expenditure of significant industry and staff resources in an effort that contributes very little to safety. The proposed rule revision would require the licensee to report all errors and changes to approved evaluation models on an annual basis. Significant errors and changes would have to be reported within 30 days. Here the definition of a significant change or error has been changed from 20°F to 50°F and the staff would have the option to decide what action would be required and the time to take this action based on the significance of the problem relative to other required licensee actions. Any change or error that results in exceeding the criteria of 50.46(b) would still be a violation of regulations and would require immediate reporting and immediate steps to bring the plant into compliance with the regulations.

ECONOMIC IMPACT OF PROPOSED RULE REVISION

The proposed revision of the ECCS rule is expected to significantly reduce the calculated PCT for large-break LOCA for most plants. Since large-break LOCA is currently limiting for most plants, this in turn would allow a reduction in restrictions on plant operation due to LOCA considerations. The most obvious operating restriction due to LOCA is allowed peak local power, but LOCA considerations also determine many other plant requirements such as equipment surveillance. Whether or not an increase in allowed peak local power would provide a benefit to a plant depends upon whether the plant operation is currently limited by LOCA considerations or other considerations such as departure from nucleate boiling (DNB). Core operating limits are complex and there is no simple answer, even for a given plant. The staff has, however, surveyed the U.S. vendors to obtain an indication of the effect of the proposed rule revision on plants of their design.

Combustion Engineering and Babcock & Wilcox plants are generally not limited in operation by LOCA peak local power

limits, but may benefit from changes to ECCS equipment requirements. Plants designed by General Electric (GE) are generally limited by LOCA considerations and stand to benefit greatly. This benefit is already available, however, through a revised GE evaluation model approved by the staff using SECY-83-472. The plants most likely to benefit from the proposed rule revision are those designed by Westinghouse. Westinghouse has indicated that most of their plants are limited in operation by LOCA considerations. Separately, the staff has surveyed Westinghouse plants by examining their allowed peaking factors. Approximately 15% of these plants were found to not be LOCA limited. This means that these plants have an allowed peaking factor of 2.32 and a calculated PCT of much less than 2200 °F. About 41% of Westinghouse plants were found to have moderate LOCA restrictions in that they have achieved the desired 2.32 peaking factor, but might have trouble maneuvering or have PCTs so close to 2200 °F so as to provide little margin for errors or plant changes such as steam generator tube plugging. The remaining 44% of Westinghouse plants were found to have significant LOCA restrictions in that they could not achieve the desired 2.32 peaking factor, had increased core monitoring requirements or have difficulty reaching full rated power.

Westinghouse has also indicated that while their plants are generally LOCA limited, they have significant margin to other limits such as DNB. Thus reduction in LOCA limits on peak power would provide benefit. This benefit could be realized through either increased allowed peaking factors or an increase in allowed total power. An increase in total power of about 5% is within the capability of existing equipment in typical Westinghouse plants. These potential benefits of the proposed ECCS rule change are significant in terms of cost savings. Improved fuel management and maneuvering can be worth several million dollars per year per plant. The present value worth (1984 dollars) of a 5% power upgrade, using conservative assumptions about remaining plant life and discount rate, ranges from as low as \$14 million to as high as \$144 million per plant depending on the age of the plant and the replacement power costs specific to that plant. A 5% power upgrade to all Westinghouse plants in the U.S. is estimated to be worth \$3.2 billion.

SAFETY IMPACT OF PROPOSED RULE REVISION

The purpose of the ECCS rule is to protect the health and safety of the public. The impact of the proposed rule revision on safety must be examined to ensure that the rule would still perform this vital function. The potential

impacts of the rule revision have been examined using probabilistic risk assessment (PRA) techniques and from a deterministic basis. Both potential increases and decreases in safety due to the proposed rule revision have been examined.

The proposed rule may allow increases in peak local power and/or total power. The PCT during a LOCA would still be required to be less than 2200°F and the other criteria met, at least with a 95% probability. It should be noted that even if there is a small probability that these criteria would be exceeded during a LOCA, this does not indicate a safety concern. The criteria, if exceeded, would only be exceeded in a small portion of the core and the criteria also contain conservatism as an additional safety factor. Thus from a deterministic point of view, the revised ECCS rule still ensures that the ECCS would protect the core during a LOCA.

The impact of the proposed rule revision was also examined from a PRA view point for several generic plants⁴. A number of potential negative risk factors were examined.

1. Higher total power would increase the inventory of fission products available for release during a severe accident by a proportional amount.
2. Higher total power may result in increased opening of relief valves and thus increased sticking of valves.
3. Modifications may be made to ECCS equipment or surveillance requirements.

All of the above negative risk factors were found to be negligible and well within the uncertainty of PRAs, with one exception. The complete removal of an ECCS system was found to result in a small increase in core melt frequency. However, this small increase in core melt frequency did not propagate through to a significant change in risk. This drastic a change in an ECCS system is also not a likely result of the proposed rule revision since ECCS systems are also required for purposes other than LOCA. Thus the negative aspects of the proposed rule change on risk are believed to be negligible.

Potential positive impacts on risk may also result from the proposed rule revision. Typical diesel generator start times as short as ten seconds are required, mainly due to large-break LOCA considerations. Best estimate calculations show that this start time may be extended significantly and still meet the 50.46(b) criteria. The

exact impact of less stringent starting and testing of diesels is unknown, but may well improve their reliability. Pressurized thermal shock (PTS) has become a concern in recent years and plants are taking steps to reduce the flux to vessel walls. Flux reduction requires higher power peaking factors which may be easier to achieve if LOCA limits are revised. Thus the ECCS rule may provide a positive impact on risk from PTS. Current ECCS systems have been designed using conservative, yet unrealistic, calculations. In some cases design decisions have been made using these unrealistic calculations that may not be optimal. Likewise, operator training and operating procedures have been based on unrealistic calculations. The NRC currently believes that safety is best served if these types of decisions are based on the best information available. The safety benefit of realistic calculations is difficult to quantify, but is believed to be real and significant.

The impact on safety of the proposed ECCS rule revision has been examined and found to have both positive and negative aspects. It is believed that there will be no net decrease in safety and potential for improvements to safety.

CONCLUSION

The NRC staff has proposed a revision to the ECCS rule to eliminate operating restrictions now known to not be necessary to protect the health and safety of the public. This rule revision will make use of over a decade of research on ECCS performance. The major benefit will be economic, but the potential also exists for improvements to plant safety as a result of a better understanding of plant behavior.

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REFERENCES

1. Federal Register, Vol. 43, No. 235 (43 FR 57157), December 6, 1978.
2. SECY-83-472, "Emergency Core Cooling System Analysis Methods," November 13, 1983, is available at the NRC public document room.
3. "Best Estimate Calculations of Emergency Core Cooling Systems Performance," Draft regulatory guide in preparation.
4. "Compendium of ECCS Research for Realistic LOCA Analysis," NUREG-1230, Draft in preparation.
5. Letter E.P. Rahe (Westinghouse) to D.F. Ross (NRC), "LOCA Margin Benefits," February 8, 1985.
6. Letter L.E. Hochreiter (Westinghouse) to W.D. Beckner (NRC), "Results of Westinghouse Calculations Using a Modified Version of Appendix K," April 23, 1984.
7. "Effect of Diesel Start Time on BWR/6 Peak Cladding Temperature", NSAC-96, January 1986.

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REGULATORY GUIDE IN SUPPORT OF ECCS RULE REVISION
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The US Nuclear Regulatory Commission staff is proposing to amend 10 CFR 50.46 and Appendix K to allow licensees to use best estimate calculations to estimate emergency core cooling system performance. This estimate in conjunction with an estimate of the uncertainty in the calculation would then be used to assure that the licensing limits set forth in 10 CFR 50.46(b) are not exceeded. The NRC staff has prepared a draft regulatory guide to assist licensees and applicants in complying with these proposed amendments. This paper sets forth the objectives of this regulatory guide, the approach taken, the difficulties encountered, and the current status of this effort.

1. Introduction

In an effort to better utilize the results of research performed on nuclear reactor thermal-hydraulic response during loss-of-coolant accidents the Nuclear Regulatory Commission staff is proposing to amend its requirements to allow a best estimate calculation of emergency core cooling system response and an estimate of the uncertainty in the calculation. In support of these proposed amendments, a draft regulatory guide has been prepared by the NRC staff to specify acceptable methods for compliance with the proposed rule changes. The rule requires that the applicable limits in 10 CFR 50.46(b) not be exceeded with "a high level of probability" but does not specify what probability level is considered acceptable by the NRC staff. Further guidance was considered appropriate for the regulatory guide because it would reduce the possibility that statistics or mathematics be litigated during legal proceedings.

The current Appendix K specifies acceptable and required features that a licensee's evaluation model should contain. The draft regulatory guide specifies best estimate features that may be used. In addition, the guide provides the framework for the methods to be used to estimate the uncertainty associated with the code calculations.

2. Approach

The regulatory guide has been structured analogous to the existing Appendix K. Conservative features which are required or acceptable have been replaced by best estimate models where possible. Conservative assumptions on plant conditions and equipment availability have been replaced by realistic assumptions. An uncertainty section was included to explain the objectives and intended use of the estimate of the accuracy of the best estimate licensing calculations. This section is general in nature and provides flexibility in compliance.

3. Models

While the known conservative models in Appendix K were not included in the regulatory guide, the staff was unable to specify acceptable best estimate models in many areas. In some cases, the acceptability of a model to predict certain thermal-hydraulic phenomena was dependent upon reactor type or fuel type and could not be specified. In other areas, acceptable models would presuppose certain numerical or modeling formulations (e.g., three-dimensional modeling or two-fluid constitutive relations) and were thus considered undesirable.

In two cases, acceptable best estimate models have been specified. The Cathcart-Pawel model for metal-water reaction rate and the ANSI/ANS 5.1-1979 Decay Heat Standard are deemed acceptable best estimate models.

In other areas, the staff sought the advice of an experts panel to propose best estimate models. The experts panel preferred instead to propose acceptable sets of experimental data and models, if possible, that fit this data acceptably well. These data and models will be published with the proposed regulatory guide and the public will be specifically asked: should the NRC specify acceptable data and models in the regulatory guide?; and, are the data and models proposed appropriate?

4. Uncertainty Estimate

The regulatory guide specifies that the acceptable probability level for meeting the licensing limits in 10 CFR 50.46(b) (e.g., calculated peak clad temperature less than 2200°F) to be 95%. The guide does not however prescribe a specific method for the uncertainty calculation. Rather, it specifies the types of simplifying assumptions that can be made, specifies the requirements for justifying those simplifying assumptions, and specifies that a two-standard deviation uncertainty may be used in lieu of calculating and considering a confidence limit (ordinarily a one-side test of hypothesis at the 95% probability level would be obtained at approximately 1.64 standard deviations).

5. Status

The regulatory guide, in the form described above, will be published as part of the rule package for comment. Specific comments have been solicited on the models and data proposed for inclusion. Comments will be considered and incorporated as appropriate.

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Experience with the Revised Appendix K

by

Brian W. Sheron
Y. Gene Hsui

Abstract

In 1983, during the course of reviewing proposed ECCS evaluation methods by General Electric, the NRC staff developed an approach to ECCS analysis which allowed for the systematic reduction of excessive margin. The staff has approved this methodology for one vendor, and is in the process of approving a methodology for another vendor.

Our experiences in licensing models to this new methodology are summarized in this paper.

In November 1983, the staff of the Office of Nuclear Reactor Regulation issued an information report to its Commission regarding how it intended to allow a new interpretation of the Appendix K requirements for ECCS analyses. This paper, entitled SECY 83-472 ⁽¹⁾, allowed for a substantial amount of the conservatism contained in current ECCS licensing models to be reduced in a systematic manner.

The method for allowing this reduction was simple and involved allowing the removal of all analysis conservatisms not specifically required in NRC regulations. ECCS analysis conservatism historically derived from four main sources. These are:

1. Required conservatisms specified in the Regulations;
2. Conservatisms identified in features specified as acceptable (though not necessarily required!) in the Regulations;
3. Conservatisms imposed in models by the NRC staff during the model review process;
4. Conservatisms imposed by the model holder (vendor or utility) for various reasons (usually a conservative, simplified model was used to eliminate the expense associated with development and use of a more complex but realistic model).

The promulgation of this approach was considered a breakthrough of sorts, in that it represented the first wholesale approach to systematically reducing excessive ECCS conservatisms. It signalled to the industry that the NRC staff was indeed serious about recognizing the large strides made in ECCS research over the past ten years. More importantly, however, it provided the approach and framework for revising the actual ECCS Rule.

In References (1) and (2), the details of the Rule revision are documented and discussed, so I will not go into them in detail in this paper. Rather, I will only touch on the highlights here and then discuss our experiences to date in implementing this new approach.

In essence, the SECY 83-472 approach, as well as the proposed ECCS Rule revision, substitutes fixed, deterministic and specified conservatisms with a statistical approach. The key elements of this approach are that:

- a. It is performance-based regulation (there are no prescriptive conservatisms in the approach);
- b. It provides incentive for using accurate, state-of-the-art analysis methods.

I will elaborate a little on the last item. This approach encourages the use of best-estimate codes, and encourages the analysis uncertainties to be

minimized. That is, the smaller the analysis uncertainty, the larger the allowable operational flexibility (e.g., higher peaking factor).

The approach is not without its drawbacks, however. In order to effectively use it, a statistical assessment of uncertainty must be performed by each user of the method. This can be a relatively expensive process, but was not intended to have to be done every time the method was applied. Moreover, a pure statistical assessment is unfortunately not possible, and an engineering estimate of the statistical uncertainty is a more appropriate way to describe the approach.

Finally, the burden of calculating and justifying the accuracy of the analysis is placed on the model user.

Since its promulgation in November 1983, we have gained experience in using this method. The first organization to use the method was General Electric. The NRC staff reviewed and approved the use of the SAFER code with this method. We also reviewed and approved the use of a statistical methodology for demonstrating the code uncertainty. Currently, the staff is in the process of reviewing best-estimate codes developed by Westinghouse and Combustion Engineering to be used with this approach, along with their statistical methods. It should be noted that although one vendor's methodology has been approved for almost two years, it has not yet been applied to a specific case. In the following two sections, a short description of our experience in licensing these models to the SECY 83-472 approach is described.

PWR Methods Experience

Both Westinghouse and Combustion Engineering have been developing best-estimate methods for large-break LOCA analyses of the Westinghouse two-loop plants having upper plenum injection (UPI). The Westinghouse best-estimate model uses a reactor system transient code, WCOBRA-TRAC, which is the Westinghouse version of COBRA-TRAC developed by Battelle Pacific Northwest Laboratory under sponsorship of the NRC. CE has not submitted their methods for staff review but has discussed their code and method development with the staff. The CE best-estimate model uses the approved CE LOCA-related codes with the exception of a new code, CEUPR, which will be used for refill and reflood analyses. Both WCOBRA/TRAC and CEUPR codes are multi-dimensional transient codes containing various mechanistic models more realistically describing physical processes of major thermal-hydraulic phenomena during a LOCA. These codes are assessed by comparison of the code calculations with relevant test data from test facilities such as FLECHT, LOFT and CCTF. Our experience in reviewing and discussing the Westinghouse and CE methods is summarized below.

(1) Uncertainty Assessment

One of the important aspects of using the SECY 83-472 approach is the uncertainty assessment. Sources or contributors to the overall uncertainty can be categorized in two parts: (1) code uncertainty which is the uncertainty inherent in the code in the field equations, various models and correlations for the prediction of various thermal-hydraulic phenomena, and the numerical

solution schemes; and (2) application uncertainty which is the uncertainty of using the code to predict the reactor system response and peak cladding temperature (PCT) during a LOCA.

Because of the complexity of the advanced codes, code uncertainty is assessed through comparisons between the experimental test data and code predictions. A statistical calculation of the differences between the measured and predicted PCTs is performed to determine the code prediction bias and variance. The following considerations are important in code uncertainty assessment: (1) To more precisely determine the code uncertainty in PCT calculations for particular plant/safety injection configurations, selections of test data should emphasize the test facilities and test configurations representative of the intended plants and safety injections configurations. For the applications of the code to plants having upper plenum injection, emphasis is placed on those test configurations such as CCTF having upper plenum injection or topdown quench to account for the effects of pooling and flooding phenomena on the upper core support plate; (2) uncertainties associated with test data including the variations of data within each data set and among data sets, and the differences between the measured and the true values due to measurement instrumentation error and lack of a temperature measurement sensor at the PCT location; and (3) scaling uncertainty due to the smaller scale of the test facilities relative to the reactor system.

Uncertainties associated with code application to reactor system LOCA analysis are those input parameters related to the plant initial and boundary

conditions, choice of two-phase flow models, correlations and coefficients, and details of system geometry nodalization, etc. These uncertainties are quantified through sensitivity studies. The sensitivity studies are performed with the best-estimate model whereas the pertinent parameters and correlations are varied within their uncertainty ranges. Sufficient sensitivity studies are needed to assess the effects of the variations of these input parameters on PCT. The results of these sensitivity studies are used to develop a statistical data base which is then used to calculate the 95th percentile value. This 95th percentile uncertainty value along with the code uncertainty and bias are used as adders to the PCT calculated with the realistic model with the nominal input values of the pertinent parameters. The resulting 95th percentile PCT is greater than the true PCT at a 95 percent probability level.

(ii) Alternative Approaches

To avoid the tedious task of calculating the 95th percentile PCT, alternative methods have been derived by Westinghouse in the implementation of SECY 83-472. Rather than using a statistical approach in determining the 95th percentile PCT, their alternative uses a deterministic approach in treating the plant parameter uncertainties. Therefore, in a realistic PCT analysis, either two-sigma values or the upper bound values of some of the plant parameters and models are used to maximize the calculated PCT. A "two-sigma" adder is determined from the thus-obtained PCT and the PCT calculated from the more realistic calculation where the nominal values of the plant parameters are

used. This is a so-called "two-sigma" approach and the resulting PCT with augmentation of code uncertainty and bias is a so-called "limit-value" PCT.

For licensing application, the PCT is calculated with the Evaluation Model (the realistic model augmented with the Appendix K requirements) and the resulting PCT must be shown to exceed the limit value PCT. The limit value PCT is higher than the 95th percentile PCT because the deterministic uncertainty treatment assumes that all pertinent parameters affecting PCT are simultaneously at their most adverse values. Therefore, when the licensing calculation PCT exceeds the limit value PCT it also exceeds the 95th percentile PCT. In another approach, the licensing PCT is calculated with the evaluation model and the input of the two-sigma or bounding values of the plant parameters. The resulting PCT would be greater than the limit value PCT. This approach resembles the traditional Appendix K evaluation model with the only difference being the use of a mechanistic best-estimate model or computer code. This is a rather conservative approach compared to the SECY 83-472 guidelines where the licensing PCT calculation would be performed with the nominal values of the plant parameters and correlations in the realistic model augmented only with the Appendix K requirements. Although it is more conservative, its advantage is the reduction in computer time required.

These and similar approaches are acceptable to the staff, and are solely a matter of choice by the model holder.

(iii) Worst Case Determination

One drawback in the SECY 83-472 approach is that the PCT calculations are performed with both a realistic model and an evaluation model. This is especially significant when sensitivity studies are performed as required by Appendix K to determine the worst conditions with respect to the power distribution, break size, noding, burnup, etc., which yield the highest PCT. Since the SECY 83-472 guideline requires the calculated PCT using the new evaluation model to be shown to exceed the 95th percentile PCT calculated with the best-estimate model, the sensitivity would necessarily be performed with both the evaluation and best-estimate models. The new evaluation model is the realistic or best-estimate model augmented with the Appendix K required features such as the Moody model for two-phase critical flow, and the use of 1.2 times the 1971 ANS standard for the fission product decay heat calculation. As a result, the calculated reactor system response to a LOCA may be different between the evaluation and best-estimate models. For example, it is possible that the PCT may occur during the blowdown period in the realistic calculation while it may occur during the reflood period in the evaluation model calculation. Therefore, the sensitivity study results performed for the evaluation model and realistic model may not be identical. In that case, different worst conditions may be used for the realistic and evaluation model calculations.

Most advanced codes require very long running times. It is therefore economically undesirable to perform sensitivity studies for both evaluation and realistic models. Because of this, engineering judgment is necessary to

determine if sensitivity study results using the realistic model are applicable to the evaluation model.

(iv) Exemptions

There are instances where some Appendix K requirements are not applicable to the specific plant configurations analyzed. In such cases, the licensees must apply for exemptions to those requirements pursuant to the provisions set forth in 10 CFR 50.12. For example, Section I.D.3 of Appendix K requires that, for the calculation of a PWR reflood rate, a carryover fraction (which is defined as the ratio of total fluid flow at the core exit plane to the total liquid flow at the core inlet plane) should be determined with applicable experimental data and be used to determine the core exit flow. This requirement was promulgated at the time when no codes were capable of accurately calculating the liquid flow rate exiting the core. The definition of carryover fraction and flow calculation method were written for conventional bottom reflood PWR plants where the inlet and outlet planes refer to the bottom and top of the core, respectively, with the fluid flowing upward on both planes. On a UPI plant having topdown reflood, the liquid enters at the top of the core and exits at both the bottom and the top of the core. The carryover fraction specified in Appendix K would not be applicable. As another example, Section I.D.5 of Appendix K requires that when reflood rates are less than one inch per second, the heat transfer calculation should be based on the assumption that cooling is only by steam and should take into account any flow blockage calculated to occur. The steam cooling requirement was set forth because the

FLECHT flow blockage tests at a bottom reflood rate of 0.6 inches per second indicated heat transfer degradation by blockage and the poor heat transfer at the low reflood rate was presumed to be the result of a lack of entrained water droplets, leaving only single phase steam cooling. This steam cooling assumption may be overly conservative for a topdown reflood plant where the entire flow situation with a combination of both upflow and downflow is different than the bottom reflood plant which is cooled by dispersed cocurrent upflow film boiling. Both core exit flow and reflood heat transfer are calculated with the advanced codes such as WCOBRA-TRAC and CEUPR. These codes will be validated with experimental data. We therefore believe there is a sound technical basis for exemption of the carryover fraction and steam cooling requirements for a UPI plant.

(v) Current Accepted Features and Codes

Specified in Appendix K to 10 CFR 50 as acceptable for an evaluation model are some critical heat flux and post CHF correlations which may be non-conservative when used outside their applicable data ranges. Included in the list of acceptable correlations is the Dougall-Rohsenow film boiling correlation without any limitation placed upon the correlation. Current heat transfer data have shown the Dougall-Rohsenow correlation to be non-conservative in some situations and have raised a question on the general acceptability of the correlation. As a result, a recommendation is being made to delete the correlation from the generally accepted correlation list. Though the continued use of the Dougall-Rohsenow correlation in the current Appendix K evaluation

models may not result in a non-conservative PCT calculation due to the inherent conservatism of the evaluation model, care should be taken in connection with the realistic or best-estimate model. If the Dougall-Rohsenow correlation is used in the SECY 83-472 approach, justification would be necessary with data comparisons to show that the correlation is not non-conservative in the range of application.

There are computer codes which have been previously accepted by the NRC for ECCS evaluation model calculations. In using these codes in the SECY 83-472 approach, a reevaluation would be needed for these codes to determine their applicability for best-estimate calculations and an uncertainty assessment is also necessary.

General Electric Methods Experience

Most of the experiences described in the previous section on PWR experience are applicable to our experiences with the General Electric model and won't be repeated in this section. There were no regulation exception requests required for the GE model. Scaling proved a challenging issue during the review, and was concluded by accepting the GE proposal to assign a 50°F conservative bias to account for it. Another relevant issue encountered in the review was the temperature range of the code verification data. Because the data only contained cladding temperature measurements up to 1600°F, the staff restricted the use of the best-estimate code up to calculated cladding temperatures of 1600°F. The approved methods applied to BWR-4s and BWR-6s.

Notable is the fact that to date no owner of a BWR-4 or -6 has applied to utilize the approved GE model and gain the operating margins routinely stated to exist.

Also notable is that the owners of two operating BWR-2s are actively pursuing this method for their plants. Due to the unique aspects of a BWR-2 (no jet pumps), they must develop a relevant data base for verifying their best-estimate code.

Summary

The initial use of a new methodology is bound to be difficult. In this case, both the NRC staff and the industry are learning how to acceptably apply the SECY 83-472 approach.

As we gain experience, I would expect both the time and effort needed to license ECCS models will decrease. The use of best-estimate codes in licensing is also a reality which the staff is encouraging.

Approval of the GE methodology shows that the approach can in fact be implemented in the regulatory process. Expected approval of the Westinghouse methodology further confirms this.

The industry must now take the lead to develop and refine the methods needed to use this approach and gain the necessary margin needed to allow nuclear plants

to run at their full potential and not be limited by ECCS considerations. No utility has yet availed itself of the margin afforded by this methodology, despite previous claims of the financial penalties imposed by ECCS analyses. We encourage the industry to now take advantage of the plant operating margins available to them and associated financial benefits so long identified with these margins.

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References

- (1) "Emergency Core Cooling System Analysis Methods," SECY 83-472, dated November 17, 1983 (NRC staff memorandum to the Commission).
- (2) Hodges, M. W.; Collins, Timothy E.; and Sheron, Brian W., "The Use of Realistic Thermal-Hydraulic Models for Licensing Applications in the United States," presented at the Third International Topical Meeting on Reactor Thermal-Hydraulics, Newport, Rhode Island, October 1985.

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STATUS OF INTEGRAL FACILITIES AND FUTURE PLANS FOR INTEGRAL TESTING

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U. S. Nuclear Regulatory Commission

Because there are a lot of changes and interesting developments in progress in our basic experimental programs, I would like to take just a few minutes to give you an overview of some of our large scale and integral systems experimental programs not covered in other sessions. I will touch briefly on the status of U. S. facilities, testing in progress during the year and our future plans.

Starting with the BWR systems experiments, our major testing facility has been the FIST facility at San Jose, California. Testing in FIST was completed in 1984 and it has remained intact and undisturbed since then. NRC has no current plans to initiate further testing in FIST. However it has been proposed by GE and DOE that the facility could be used for experiments related to advanced BWR designs. No decision on the proposal has been made.

Semiscale and LOFT are the principle U. S. facilities used for transient investigations in Westinghouse-type PWRs. Both facilities are now shutdown. Semiscale completed its testing program early in 1986 with a series of small break LOCAs without high pressure coolant injection. After testing was completed Semiscale was placed in a cold shutdown status with minimal steps taken to retard degradation. It remains in this condition and no future plans for testing are defined.

There is testing in progress in the ROSA IV facility in Japan which is of great interest to NRC. We take an active role in recommending appropriate experiments to include in the ROSA experimental plan and we also actively analyze the ROSA results. Tests in other foreign facilities such as LOBI, PKL and BETHSY are also of great interest to NRC.

Transient responses of B&W designed reactors are of great concern to NRC because of their frequency and complexity. The B&W Owners Group has taken the lead role in reevaluating the safety of these plants. Currently the experimental data base applicable to transient response of B&W plants is significantly less complete than that available for either BWR or Westinghouse-designed PWRs, and therefore the major thrust of NRC's future thermal hydraulic research program relates to B&W designs. A report is nearing completion which defines NRC's plans for future thermal hydraulic research on B&W reactor designs, with options considered depending on the availability of NRC funding and outside support. However this plan is being implemented now and I will briefly present the experimental portion, including both existing facilities, MIST and UMCP, and future experimental facilities which we refer to as continuing experimental capabilities, of CEC, the principal purpose of which is to assure NRC of direct access experimental facilities in the future.

Testing is in progress in the MIST facility at the B&W Alliance, Ohio site. This is the testing program for which the facility was originally designed and is directed mainly at small break LOCA and natural circulation phenomena. MIST is sponsored by NRC, EPRI, B&W and the B&W Owners Group and the jointly sponsored program is scheduled to be completed early this fiscal year.

NRC is interested in follow-on testing in MIST during 1987 through 1989, principally related to transients other than SBLOCA. We have held discussions with the other MIST sponsors concerning their willingness to continue joint sponsorship of some future testing. It appears likely that some follow-on testing will be performed in MIST although the scope of these tests and their number will depend on the action taken by the other sponsors.

Testing is in progress in the University of Maryland (UMCP) experimental facility. This facility is a major departure from the design concepts of earlier facilities which were tall and skinny in order to preserve prototype reactor elevations. The design introduces complexities into the system response not present in facilities such as MIST, but it is hoped that it can also contribute to our understanding of operating reactor response in ways not possible in MIST.

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My last topic will address NRC plans for future experimental facilities. These plans are currently directed solely toward B&W plant responses.

Our initial focus is on the B&W OTSG response, particularly the complex fluid flow and heat transfer processes present during emergency feedwater spray operation. To date we have developed an experimental approach and facility concepts which would involve separate effects testing starting in 1987 and ending in 1989. The current plan would involve three facilities, i.e.,

1. a single tube air/water facility which would be tested both with and without tube heating,
2. a 1/8 sector full scale model air/water experimental facility,
3. a facility similar to the 1/8 sector full scale design capable of reduced pressure steam/water experiments and freon experiments.

We are also working on an integral facility which we expect to use a new scaling approach which will enhance multidimensional phenomena and response characteristics representative of large plants and provide a significant new and realistic challenge to our systems codes. In the limit, this leads to a photographic reduction of the prototype plant which we refer to as linear scaling. Our plan for 1987-1988 is to select a candidate concept upon which we will perform tradeoff studies to optimize facility response (for example tradeoffs among system pressure, system volume and power level modeled). We will also perform systems code analyses of facility response, develop a conceptual design with support facility requirements and estimate the facility cost. The schedule for the integral facility is dependent on its cost the availability of funding for thermal hydraulic experiments and the cost of the OTSG experiments which are regarded as higher priority.

I hope that it is apparent from this summary that our experimental program is changing to meet new demands and that our future programs offer both great potential for improved understanding of reactor transient responses and great challenges of design, operation and analysis.

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EVALUATION OF SCALING CONCEPTS FOR INTEGRAL SYSTEM TEST FACILITIES^a

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ABSTRACT

A study was conducted by EG&G Idaho, Inc., to identify and technically evaluate potential concepts which will allow the United States Nuclear Regulatory Commission to maintain the capability to conduct future integral, thermal-hydraulic facility experiments of interest to light water reactor safety. This paper summarizes the methodology used in the study and presents rankings for each facility concept relative to its ability to simulate phenomena identified as important in selected reactor transients in Babcock & Wilcox and Westinghouse large pressurized water reactors. Established scaling methodologies are used to develop potential concepts for scaled integral thermal-hydraulic experiment facilities. Concepts selected included: full height, full pressure water; reduced height, reduced pressure water; reduced height, full pressure water; one-tenth linear, full pressure water; and reduced height, full scaled pressure Freon. Results from this study suggest that a facility capable of operating at typical reactor operating conditions will scale most phenomena reasonably well. Local heat transfer phenomena is best scaled by the full height facility while the reduced height facilities provide better scaling where multi-dimensional phenomena are considered important. Although many phenomena in facilities using Freon or water at nontypical pressure will scale reasonably well, those phenomena which are heavily dependent on quality can be distorted. Furthermore, relation of data produced in facilities operating with nontypical fluids or at nontypical pressures to large plants will be a difficult and time-consuming process.

INTRODUCTION

The United States Nuclear Regulatory Commission (NRC) is currently assessing future light water reactor safety research needs. Such an assessment is desirable since many existing NRC research programs will be completed in the near future and experimental facilities associated with these programs will be shut down. Both separate effects and integral experiment needs are being examined to determine what continuing experimental capability will be necessary to enable NRC to maintain thermal-hydraulic testing capability so that existing and future licensing issues can be experimentally addressed. As part of this overall effort, EG&G

a. Work supported by the U.S. Nuclear Regulatory Commission, Office of Nuclear Regulatory Research, under DOE Contract No. DE-AC07-76ID01570.

Idaho, Inc., was requested to conduct a study to identify and technically evaluate potential scaled concepts that will provide NRC capability in the future to conduct integral thermal-hydraulic experiments of interest to reactor safety. This paper addresses the following objectives:

1. Identification of reactor transients of major importance to reactor safety issues.
2. Identification of phenomena within these selected transients that are of significant importance or influence.
3. Identification and review of existing and commonly used scaling methodologies and criteria employed in the design of small scale integral facilities for the purpose of evaluating advantages and disadvantages of each methodology.
4. Based on the results of Item (3), identification of potential integral facility concepts which will meet NRC needs for a continuing experimental capability.
5. Technical evaluation of the scaled facility concepts identified in (4) to evaluate their ability to preserve the phenomena identified in (2).

The objective of this study is to determine the technical merits of each of the selected potential scaled integral facility concepts as it relates to the individual physical phenomena.

The methodology employed in the evaluation is as follows. First, probabilistic risk assessments, plant operating history, computer code analyses, and International Code Assessment Program matrices were examined to identify transients and thermal-hydraulic phenomena during these transients that are of interest and/or importance to reactor safety. Results of this effort produced matrices of thermal-hydraulic phenomena and base transients that an integral facility concept should preserve or be able to conduct. Results of this effort also provided information to allow base plant selections for Babcox & Wilcox (B&W) and Westinghouse (W) pressurized water reactor (PWR) vendors. Scaling methods utilized in the construction of small-scale thermal-hydraulic facilities were then applied using information from the selected base plants to generate parameters for five scaled integral facility concepts. Local scaling analyses of each concept were then conducted to evaluate the technical merits of each scaled facility concept on its ability to preserve phenomena relative to that expected in a full scale plant. Each scaled facility concept was then rated in its ability to conduct the transients identified in light of conclusions reached from the scaling analyses and capabilities of the scaled facility concept.

One of the general guidelines for the present study was that major use of any scaled facility will be for the simulation of plant transients and that minimization of problems associated with drawing inferences about plant behavior from scaled facilities is desirable. It should also be

pointed out that the ranking of the scaled facility concepts is based on their ability to simulate the individual physical phenomena and no effort has been made in this study to examine the synergistic effects. Such evaluation will require the application of system computer codes to the scaled facility concepts.

PHENOMENA IDENTIFICATION

Five transients were identified that a scaled integral facility should be able to simulate. These transients were identified based on potential significance relative to reactor safety and the production of a wide range of thermal-hydraulic phenomena. The thermal-hydraulic transients of potential significance to reactor safety were obtained from two sources: safety analysis reports (SARs)¹ and probability and risk assessments (PRAs).^{2,3}

The reviews of both the SAR events and the risk-dominant events yielded similar lists of significant thermal-hydraulic transients. These transients included (a) loss-of-coolant accident (LOCA), which is a subset of decrease in reactor coolant inventory, (b) anticipated transients without scram (ATWS), and (c) total loss of heat sink, which is a subset of decrease in heat removal by the secondary system. Transients initiated by an increase in heat transfer to the secondary system, such as a steam line break, are also of potential interest. These significant transients are summarized in Table 1. Two LOCA transients, a small break LOCA (SBLOCA) and a large break LOCA (LBLOCA), are listed in Table 1. While the SBLOCA is more significant relative to risk, the LBLOCA is included because of the unique thermal-hydraulic phenomena produced in such a transient.

TABLE 1. SIGNIFICANT THERMAL-HYDRAULIC TRANSIENTS

Transient Category	Transient
Decrease in reactor coolant inventory	SBLOCA LBLOCA
Anticipated Transients Without Scram	Loss of feedwater with failure to scram
Decrease in heat removal by the secondary system (total loss of heat sink)	Station blackout
Increase in heat removal by the secondary system	Steam line break

The important thermal-hydraulic phenomena were identified in the significant transients relative to reactor safety to determine the phenomena that are important to simulate in the scaled integral facility. These phenomena are summarized in Table 2 for a W plant type and Table 3 for a B&W plant type. The thermal-hydraulic phenomena were based on code assessment matrices proposed by the Committee on Safety of Nuclear Installations. The phenomena not identified as high or medium importance were

TABLE 2. WESTINGHOUSE TRANSIENT/PHENOMENA MATRIX

[illegible]

H - high importance of phenomena during transient
M - medium importance of phenomena during transient
¹Important in sealed facilities

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TABLE 3. B&W TRANSIENT/PHENOMENA MATRIX

[illegible]

¹Important in scaled facilities

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judged to be unimportant. The importance of the thermal-hydraulic phenomena was evaluated subjectively based on a review of thermal-hydraulic code calculations made using the RELAP⁴ and TRAC⁵ computer codes, evaluation of data from integral system experiments such as LOFT⁶ and Semiscale,⁷ and other topics of research and is affected by the assumed sequence of the transient. For example, pump behavior is listed as low in importance during a SBLOCA because the reactor coolant pumps would normally be tripped early in the transient. The pump behavior would become highly important if the pumps were not tripped.

SCALING METHODOLOGIES AND SCALED FACILITY CONCEPTS

The work of Ishii et al.,^{8,9,10} was used as the basis for the development of the scaling relationships which were used in this study. Scaled parameter ratios were developed for single-phase forced circulation, single-phase natural circulation, and two-phase natural circulation. These parameter ratios are presented in Table 4. The subscript "R" denotes the ratio between the model parameter and the plant parameter and the subscript "o" denotes a reference component, generally selected to be the core.

The parameter ratios shown in Table 4 include both fluid property ratio terms and geometric ratio terms. The property ratio terms provide the scaling basis for using different fluids and/or different operating conditions between the model and the plant. Note from Table 4 that if all of the property ratios are unity, i.e., if the same fluid and operating conditions are used in the plant and model, then the single-phase forced circulation, the single-phase natural circulation, and the two-phase natural circulation relationships are identical. If the property ratios are not unity, then discontinuities will exist between the various modes of operation. For the single-phase natural circulation case, either the core temperature difference or the core power must be specified in order to define the balance of the parameter ratios, consequently the two-phase natural circulation core power was specified for the single-phase natural circulation core power in the development of Table 4.

The generalized parameter ratios presented in Table 4 can be used to define scaled systems with specific geometric requirements. The popular full height volume scaled system parameter ratios are obtained by specifying a length ratio of unity. If like fluids and operating conditions are used, then the time, velocity, power/volume, and heat flux ratios are also unity and the core power ratio is equal to the volume ratio, thus the basis of the term "volume scaled" for this specific case. Linear scaling is obtained by specifying the diameter ratio to be equal to the length ratio, resulting in a system in which the time, velocity, power/volume, and heat flux ratios are no longer unity.

Five scaled facility concepts were developed using the generalized parameter ratios from Table 4 and based on the Oconee PWR for B&W type plants and the Seabrook PWR for W type geometry. The five concepts are:

1. Full Height Full Pressure Water (FHFPW). This concept was selected for evaluation because of the numerous facilities which have been built using this concept such as Semiscale and MIST.¹¹

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TABLE 4. PARAMETER RATIOS FOR 10 NATURAL AND FORCED CIRCULATION AND 20 NATURAL CIRCULATION

Parameter	Symbol	Parameter Ratios		
		1 ϕ Forced Circulation	1 ϕ Natural Circulation	2 ϕ Natural Circulation
Length	l_{oR}	l_{oR} (defined)	l_{oR} (defined)	l_{oR} (defined)
Diameter	d_{oR}	d_{oR} (defined)	d_{oR} (defined)	d_{oR} (defined)
Area	a_{oR}	d_{oR}^2	d_{oR}^2	d_{oR}^2
Volume	v_{oR}	$a_{oR} l_{oR}$	$a_{oR} l_{oR}$	$a_{oR} l_{oR}$
Core ΔT	ΔT_{oR}	ΔT_{oR} (defined)	$\left(\frac{1}{\beta_R}\right)^{\frac{1}{3}} \left(\frac{h_{fgR}}{C_{pR}}\right)^{\frac{2}{3}} \left(\frac{\rho_g}{\rho_{fg}}\right)^{\frac{2}{3}}$	$\left(\frac{\rho_g}{\rho_{fg}}\right)_R \left(\frac{\rho_f}{\rho_o}\right)_R \frac{h_{fgR}}{C_{pR}}$
Velocity	u_{oR}	$\sqrt{l_{oR}}$	$\left[\beta_R \left(\frac{\rho_g}{\rho_{fg}}\right)_R \frac{h_{fgR}}{C_{pR}}\right]^{\frac{1}{3}} \sqrt{l_{oR}}$	$\sqrt{l_{oR}}$
Time	t_R	$\sqrt{l_{oR}}$	$\sqrt{l_{oR}} \left[\beta_R \left(\frac{\rho_g}{\rho_{fg}}\right)_R \frac{h_{fgR}}{C_{pR}}\right]^{-\frac{1}{3}}$	$\sqrt{l_{oR}}$
Gravity	g_R	1	1	1
Power/Volume	q_{oR}'''	$\left(1/\sqrt{l_{oR}}\right) \Delta T_{oR} \rho_{oR} C_{pR}$	$\left(1/\sqrt{l_{oR}}\right) \left(\frac{\rho_g}{\rho_{fg}}\right)_R \rho_{fR} h_{fgR}$	$\left(1/\sqrt{l_{oR}}\right) \left(\frac{\rho_g}{\rho_{fg}}\right)_R \rho_{fR} h_{fgR}$
Heat Flux	q_{oR}''	$\left(1/\sqrt{l_{oR}}\right) \Delta T_{oR} \rho_{oR} C_{pR}$	$\left(1/\sqrt{l_{oR}}\right) \left(\frac{\rho_g}{\rho_{fg}}\right)_R \rho_{fR} h_{fgR}$	$\left(1/\sqrt{l_{oR}}\right) \left(\frac{\rho_g}{\rho_{fg}}\right)_R \rho_{fR} h_{fgR}$
Core Power	q_{oR}	$a_{oR} \sqrt{l_{oR}} \Delta T_{oR} \rho_{oR} C_{pR}$	$a_{oR} \sqrt{l_{oR}} \left(\frac{\rho_g}{\rho_{fg}}\right)_R \rho_{fR} h_{fgR}$	$a_{oR} \sqrt{l_{oR}} \left(\frac{\rho_g}{\rho_{fg}}\right)_R \rho_{fR} h_{fgR}$
Rod Diameter	RD_R	1	1	1
Number of Rods	NR_R	a_{oR}	a_{oR}	a_{oR}
Flow Rate	$\dot{m}_{oR}$	$a_{oR} \sqrt{l_{oR}} \rho_{oR}$	$\left[\beta_R \left(\frac{\rho_g}{\rho_{fg}}\right)_R \frac{h_{fgR}}{C_{pR}}\right]^{\frac{1}{3}} \sqrt{l_{oR}} \rho_{oR} a_{oR}$	$a_{oR} \sqrt{l_{oR}} \rho_{oR}$
ΔH Subcooling		---	---	$\left(\frac{\rho_g}{\rho_{fg}}\right)_R h_{fgR}$
ΔT Subcooling	ΔT_{subR}	ΔT_{oR}	ΔT_{oR}	---
Pump Head	ΔH_{dR}	l_{oR}	---	---
Friction Number	F_R	1	1	1

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2. Reduced Height Reduced Pressure Water (RHRPW). This concept was also motivated by existing facilities, specifically the facilities at the University of Maryland College Park¹² and Stanford Research Institute.¹³ The lower pressure and reduced height offers potential for reduced initial construction costs and operation costs.
3. Reduced Height Full Pressure Water (RHFPW). This concept was considered as a possibility in order (a) to reduce the problems introduced by unlike fluid and operating conditions, (b) to potentially lower costs by reducing height and volume, and (c) to provide a closer to ideal pressure distribution in the primary system piping than can be obtained in the FHFPW concept.
4. One-Tenth Linear Full Pressure Water (LFPW). The linear concept is actually a subset of the RHFPW concept but was included because of the perceived ability of this concept to provide good modeling of multi-dimensional behavior.
5. Reduced Height Full Scaled Pressure Freon (RHFPF). This concept was selected to allow the evaluation of phenomena associated with using a nonwater medium. The use of Freon allows for the use of the smallest system investigated, potentially lowering construction costs and power requirements.

Ideal geometric parameters were developed for each of the scaled facility concepts based on the Oconee PWR for B&W type plants as shown in Table 5 and based on the Seabrook PWR for W type plants as shown in Table 6. For the FHFPW concepts the MIST facility was used for the B&W type geometry and Semiscale was used for the W geometry.

The terms "Ideal" scaled and "Actual" scaled are introduced here because they are referred to in Tables 5 and 6 and will be used often throughout the remainder of the paper. Ideal scaling in this paper refers to a theoretical facility which is built to the exact geometric ratios as defined in Table 4 and does not deviate from them in any component throughout the entire system as opposed to an actual facility in which the ideal dimensions or conditions may have been modified to account for local scaling distortions.

LOCAL PHENOMENA SCALING AND CONCEPT RANKING

Individual phenomena were investigated for the five potential facility concepts described by Tables 5 and 6. In addition the "Actual" FHFPW concept was investigated, the actual concept being the Semiscale facility for the W type plant and the MIST facility for the B&W type plant. Phenomena were evaluated for each of the important thermal-hydraulic transients identified in Table 1. However, the majority of the evaluations were performed for a SBLOCA, which was identified as the most significant transient from a phenomenological point of view.

TABLE 5. CONCEPTUAL SCALED FACILITY PARAMETERS BASED ON B&W OCONEE PWR

Parameter	Oconee	FHFPW ^a	RHRPW	RHFPW	LFPW	RHFPF
Length Ratio	1.0	1.0	.375	.342	.10	.375
Diameter Ratio	1.0	1/28.6	1/10	1/15.35	1/10	1/20
Area Ratio	1.0	1/818	1/100	1/235.7	1/100	1/400
Volume Ratio	1.0	1/818	1/266.7	1/689	1/1000	1/1067
Core Temperature Difference Ratio	1	1	.35	1	1	1
Time Ratio	1	1	.6124	.5848	.3162	.6124
Primary Pressure (psia)	2170	2170	435	2170	2170	432.5
Primary Flow Rate (lbm/hr x 10 ⁻⁶)	131.3	.161	.937	.326	.415	.329
Primary System Volume (ft ³)	11396	13.9	42.7	16.5	11.4	10.7
Hot Leg Diameter (in)	36	1.26	3.6	2.34	3.6	1.8
Vessel ID (in)	170.6	5.97	17.06	11.11	17.06	8.53
Number of Heater Rods	36816	45	368	156	368	92
Heater Rod Length (in)	144	144	54.0	49.25	14.4	54.0
Power/Volume (KW/ft ³)	225.3	225.3	118.37	385.26	712.4	109.6
Core Power (MW)	2568	3.2	5.06	6.37	8.12	1.17
Ave Heat Flux (Btu/hr-ft ² x 10 ⁻⁵)	1.715	1.715	.901	2.93	5.42	.834
Secondary Pressure (psia)	924.5	924.5	185.3	924.5	924.5	184.2
Number of Steam Generators	2	2	2	2	2	2
Number of SG Tubes/Generator	15531	19	155	66	155	39
SG Tube Length (ft)	52.1	52.1	19.54	17.82	5.21	19.54
SG Shell ID (in)	137.9	4.82	13.8	8.98	13.8	6.90
Feedwater Flow Rate (lbm/hr x 10 ⁻⁴)	560	.685	.856	1.38	1.77	2.9
Pressurizer Volume (ft ³)	1518.5	1.86	5.69	2.2	1.52	1.42

a. The FHFPW parameter values reflect Ideal MIST scaling.

TABLE 6. CONCEPTUAL SCALED FACILITY PARAMETERS BASED ON WESTINGHOUSE SEABROOK PWR

Parameter	Seabrook	FHFPW ^a	RHRPW	RHFPW	LFPW	RHFPF
Length Ratio	1.0	1.0	.375	.375	.10	.375
Diameter Ratio	1.0	1/41.3	1/11.81	1/13.89	1/10	1/16.87
Area Ratio	1.0	1/1705	1/139.4	1/193	1/100	1/285.6
Volume Ratio	1.0	1/1705	1/371.8	1/514.7	1/1000	1/758.8
Core Temperature Difference Ratio	1	1	.35	1	1	1
Time Ratio	1	1	.6124	.6124	.3162	.6124
Primary Pressure (psia)	2250	2250	435	2250	2250	448.5
Primary Flow Rate (lbm/hr x 10 ⁻⁶)	140.3	.0823	.718	.445	.444	.494
Primary System Volume (ft ³)	11524	6.76	30.99	22.39	11.5	15.19
Hot Leg Diameter (in)	29	.702	2.456	2.088	2.9	1.719
Vessel ID (in)	173	4.22	14.64	12.46	17.3	10.26
Number of Heater Rods	50952	30	365	264	510	179
Heater Rod Length (in)	143.7	143.7	53.89	53.89	14.37	53.89
Power/Volume (KW/ft ³)	296	296	155.5	483.4	936	143.9
Core Power (MW)	3411	2.00	4.82	10.82	10.79	2.195
Ave Heat Flux (Btu/hr-ft ² x 10 ⁻⁵)	1.898	1.898	.997	3.099	6.001	.9229
Secondary Pressure (psia)	1000	1000	193.3	1000	1000	199.3
Number of Steam Generators	4	4	4	4	4	4
Number of SG Tubes/Generator	5626	4	40	29	56	20
SG Tube Length (ft)	54.0	54.0	20.2	20.2	5.40	20.2
SG Shell ID (in)	129.4	3.13	10.96	9.32	12.9	7.67
Feedwater Flow Rate (lbm/hr x 10 ⁻⁴)	378.5	.22	.454	1.20	1.20	2.63
Pressurizer Volume (ft ³)	1800	1.06	4.84	3.50	1.80	2.37

a. The FHFPW parameter values reflect Ideal Semiscale scaling.

Phenomena investigated were:

Natural Circulation

Two-Phase Pump Flow

Critical Flow

Flow Regimes

Pressure Change

Flooding and Reflux Transitions

Void Fraction-Quality Relationship

Heat Transfer

 Dryout type core uncover

 Critical heat flux

 Condensation heat transfer

 Single-phase liquid heat transfer

 Film boiling heat transfer

 Rewet

Multi-dimensional Effects

Emergency Core Cooling (ECC) Mixing and Condensation

Core Bypass

Pressurizer Hydraulics

Accumulator Hydraulics

Structural Heat and Heat Loss

The phenomena were evaluated on a local or separate effects basis. The general procedure was to determine how the phenomena actually scaled in the scaled facility and how the phenomena should ideally scale based on the relationships given in Table 4. The distortion in the scaling was then determined by a comparison of the actual and ideal scaling. A numerical ranking of 1 to 4, where a 1 indicates no distortion and a 4 indicates unacceptable distortion, was assigned to each concept to qualitatively indicate its ability to scale each phenomena. The relative differences between the scaling concepts were then evaluated. The rankings applied to each scaled facility concept are summarized in Tables 7 through 12 for the W type plant and Tables 12 through 16 for the B&W type plant. Each table pertains to a single transient.

The rankings presented in Tables 7 through 16 have several limitations that should be understood in order to properly interpret the results. First, distortion factors were not obtained in the evaluation of all the local phenomena. As a result, some of the numerical rankings were determined subjectively. Second, it is difficult to reduce to a single number the capability of a model to scale complex thermal-hydraulic phenomena. For example, the phenomena of reflux cooling is affected by

TABLE 7. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT STEAM LINE BREAK PHENOMENA IN A WESTINGHOUSE LPWR

Importance ²		H	H	M	M	M	H	H	M	M	M	M	H	M	H	M	M					
Phenomena	Scaling Concept	Break Flow	1-Phase NC	Phase Separation/Slip	Upper Head	SG Secondaries	Heat Transfer	Covered Core	SG Primary	SG Secondary	Multidimensional Effects	Assym. Loop response	Core	ECC Mixing & Cond.	Upper Head Mix & Cond.	Structural Heat ³	Heat Loss ³	Separator Behavior	Pressurizer T. H.	Decay Heat	Neutronics	Boron Transport
		1	4	2	1	1	1	1	2	4	1	4	3	4	3	1	1	3	3			
FHFPW	Ideal	1	4	2	1	1	1	1	2	4	1	4	3	4	3	1	1	3	3			
	Actual	1	1	1	1	1	1	1	2	4	1	3	3	3	3	1	1	3	3			
RHRPW		2	3	3	2	2	2	2	1	2	3	2	2	2	3	3	1	3	2			
RHFPW		1	1	2	2	2	2	2	1	2	1	2	2	2	3	1	1	3	2			
	.1 Linear	1	1	2	2	3	3	2	1	1	1	1	2	2	3	1	1	3	1			
RHFPF		2	2	1	1	3	3	2	1	2	2	2	2	2	3	2	1	3	2			

1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

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TABLE 8. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT LOSS OF HEAT SINK TRANSIENT PHENOMENA IN A WESTINGHOUSE LPWR

Importance ²		M	H	H	M	M	M	M	H	M	H	M	H
Phenomena	Scaling Concept	1-Phase Nc	Phase Separation/Slip	Core	Upper Plenum	Surge Line CCFL	Heat Transfer	Covered Core	Uncovered Core	CHF	SG Primary	SG Secondary	Valve Behavior/Leakage
													Decay Heat
FHFPW	Ideal	4	1	2	2	1	1	1	1	1	1	1	1
	Actual	1	1	1	2	1	1	1	1	1	1	1	3
RHRPW		3	2	3	3	2	3	4	2	2	2	2	1
RHFPW		1	2	2	2	2	2	2	2	2	2	1	1
	.1 Linear	1	2	2	3	2	3	2	3	2	1	1	2
RHFPF		2	1	1	1	2	2	2	3	2	2	2	1

1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 important in scaled facilities

MDH01242

TABLE 9. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT ATWS PHENOMENA IN A WESTINGHOUSE LPWR

Importance ²		H	M	M	M	H	H	M	H	M	H			
Phenomena	Scaling Concept	Phase Separation/Slip	Core Transfer	Covered Core	Uncovered Core	CHF	SG Primary	SG Secondary	Multidimensional Effects	Core	Neutronics	Boron Transport	Structural Heat	Heat Loss
FHFPW	Ideal	1	1	1	1	1	1	1	4	3	3	3	4	3
	Actual	1	1	1	1	1	1	1	4	3	3	3	3	3
RHRPW		2	2	3	4	2	2	2	2	3	3	2	2	2
RHFPW		2	2	2	2	2	2	2	2	3	3	2	2	2
	.1 Linear	2	2	3	2	3	2	2	1	3	3	2	2	2
RHFPPF		1	2	2	3	3	2	2	2	3	3	2	2	2

1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

MOHO 1243

TABLE 10. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT SBLOCA PHENOMENA IN A WESTINGHOUSE LPWR

Importance ²		H	M	M	M	H	M	H	M	M	H	M	H	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M
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1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

MGH01244

TABLE 11. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT LBLOCA PHENOMENA IN A WESTINGHOUSE LPWR

Importance ²		H	M	M	M	H	H	H	H	M	M	M	M	H	M	H	M	M	M	H	M	H	M	H	M		
Phenomena	Scaling Concept	Break Flow	Phase Separation/Slip	Core	Downcomer	Upper Plenum	Downcomer CCFL	Heat Transfer	Uncovered Core	CHF	Rewet	Reflood	SG Primary	SG Secondary	Multidimensional Effects	Assym. Loop Response	Downcomer	Core	ECC Mixing & Cond.	Surge Line Hydraulics	Pump Behavior	Decay Heat	UHI	Accumulator Hydraulics	Structural Heat ³	Heat Loss ³	
		Ideal	1	1	1	1	3	1	1	1	1	1	1	1	1	2	4	4	1	4	2	1	1	1	1	3	4
FHFPW	Actual	1	1	1	1	1	1	1	1	1	1	1	1	1	1	2	4	4	1	3	2	1	1	1	1	3	3
RHRPW		2	2	2	2	3	3	4	3	3	3	3	3	3	1	2	2	3	4	3	1	1	1	1	1	2	2
RHFPW		1	2	2	2	1	2	2	3	3	2	2	2	2	1	2	2	1	2	2	1	1	1	1	1	2	2
RHFPW	.1 Linear	1	2	2	2	2	3	2	3	3	3	3	3	3	1	1	1	1	4	2	1	1	1	1	1	2	2
RHFPF		2	1	1	1	2	2	3	3	3	3	2	2	2	1	2	2	2	4	3	1	1	1	1	1	2	2

1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

MGH01245

TABLE 12. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT STEAM LINE BREAK PHENOMENA IN A B&W LPWR

Importance ²		H	H	M	M	H	M	H	M	M	H	M	M	H	M	M	M	H	M	M				
Phenomena	Scaling Concept	Break Flow	1-Phase NC	Phase Separation/Slip	Upper Head	SG Secondaries	CCFL SG Support Plates	Heat Transfer	Covered Core	SG Primary	SG Secondary	Multidimensional Effects	Assym. Loop Response	Core	Auxiliary Feedwater	ECC Mixing & Cond.	Upper Head Mix & Cond.	Structural Heat ³	Heat Loss ³	Vent Valve Behavior	Pressurizer T. H.	Decay Heat	Neutronics	Boron Transport
		Ideal	1	4	2	1	1	1	1	1	1	1	1	4	4	1	4	3	4	3	1	1	1	3
FHFPW	Actual	1	1	1	1	1	1	1	1	1	1	1	4	4	1	3	3	3	1	1	1	3	3	3
RHRPW		2	3	3	2	3			2	2	2		1	2	2	3	2	2	2	2	3	1	3	2
RHFPW		1	1	2	2	2			2	2	2		1	2	2	1	2	2	2	2	1	1	3	2
	.1 Linear	1	1	2	2	3			3	3	2		1	1	1	1	1	2	2	2	1	1	3	1
RHFPF		2	2	1	1	1			3	3	2		1	2	2	2	2	2	2	2	2	1	3	2

1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

MGH01246

TABLE 13. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT LOSS OF HEAT SINK TRANSIENT PHENOMENA IN A B&W LPWR

Importance ²		M	H	H	M	M	M	H	H	M	M	H	H				
Phenomena	Scaling Concept	1-Phase Nc	Phase Separation/Slip	Core	Upper Plenum	Surge Line CCFL	Heat Transfer	Covered Core	Uncovered Core	CHF	SG Primary	SG Secondary	Vent Valve Behavior	Valve Behavior/Leakage	Decay Heat	Structural Heat ³	Heat Loss ³
FHFPW	Ideal	4		1	2	2		1	1	1	1	1	3	1	1	3	4
	Actual	1		1	1	2		1	1	1	1	1	1	1	1	3	3
RHRPW		3		2	3	3		2	3	4	2	2	2	2	1	2	2
RHFPW		1		2	2	2		2	2	2	2	2	2	1	1	2	2
	.1 Linear	1		2	2	3		2	3	2	3	2	2	1	1	2	2
RHFPF		2		1	1	1		2	2	2	3	2	2	2	1	2	2

1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

MDH01247

TABLE 14. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT ATWS PHENOMENA IN A B&W LPWR

Importance ²		H	M	M	M	H	H	M	H	H	M	H	M	H	
Phenomena	Scaling Concept	Phase Separation/Slip	Core	Heat Transfer	Covered Core	Uncovered Core	CHF	SG Primary	SG Secondary	Multidimensional Effects	Core	Neutronics	Boron Transport	Structural Heat ³	Heat Loss ³
FHFPW	Ideal	1	1	1	1	1	1	1			4	3	3	3	4
	Actual	1	1	1	1	1	1	1			4	3	3	3	3
RHRPW		2	2	3	4	2	2				2	3	3	2	2
RHFPW		2	2	2	2	2	2				2	3	3	2	2
	.1 Linear	2	2	3	2	3	2				1	3	3	2	2
RHFPF		1	2	2	3	3	2				2	3	3	2	2

1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

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TABLE 15. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT SBLOCA PHENOMENA IN A B&W LPWR

Importance ²		H	M	M	M	H	M	M	M	H	M	H	H	M	M	H	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M	M
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1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

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TABLE 16. RANKING¹ OF SCALING CONCEPT CAPABILITY TO SIMULATE IMPORTANT LBLOCA PHENOMENA IN A B&W LPWR

Importance ²		H	M	M	M	H	H	H	H	M	M	M	M	H	M	M	H	H	M							
Scaling Concept	Phenomena	Break Flow	Phase Separation/Slip	Core	Downcomer	Upper Plenum	Downcomer CGFL	Heat Transfer	Uncovered Core	CHF	Rewet	Reflood	SG Primary	SG Secondary	Multidimensional Effects	Asym. Loop Response	Downcomer	Core	Auxiliary Feedwater	ECC Mixing & Cond.	Surge Line Hydraulics	Pump Behavior	Decay Heat	Accumulator Hydraulics	Structural Heat ³	Heat Loss ³
FHFPW	Ideal	1	1	1	1	3			1	1	1	1	1	1	1	1	4	4	4	4	4	2	1	1	3	4
	Actual	1	1	1	1	1			1	1	1	1	1	1		1	4	4	4	1	3	2	1	1	3	3
RHRPW		2	2	2	2	3		3	4	3	3	3	3	3		1	2	2	2	3	4	3	1	1	2	2
RHFPW		1	2	2	2	1		2	2	3	3	2	2			1	2	2	2	1	2	2	1	1	2	2
	.1 Linear	1	2	2	2	2		3	2	3	3	3	3			1	1	1	1	1	4	2	1	1	2	2
RHFPF		2	1	1	1	2		2	3	3	3	3	2	2		1	2	2	2	2	4	3	1	1	2	2

1 Ranking (1=Best, 4=Not Acceptable)

2 Importance (H=High Importance, M=Medium Importance)

3 Important in scaled facilities

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many different aspects including core heat transfer, condensation heat transfer, flooding, hot leg stratified flow, flow regime transitions, etc. The concept rankings could be altered if the rankings were based on a different aspect of the phenomena.

Overall average rankings for all the transients for both types of plants are presented in Table 17. These rankings represent a simple averaging of the rankings presented in Tables 7 through 16 with no weighting for importance of phenomena.

TABLE 17. AVERAGE OVERALL RANKINGS FOR SCALED FACILITY CONCEPTS

<u>Facility</u>	<u>Average Ranking</u>
FHFPW - Ideal	2.12
FHFPW - Actual	1.67
RHRPW	2.52
RHFPW	1.90
LFPW	2.05
RHFPPF	1.95

Table 17 indicates that the actual FHFPW concept provides the best representation of the largest number of phenomena which were investigated while the RHRPW concept represented the least number of phenomena. The RHFPW, LFPW, and RHFPPF concepts ranked closely together between the actual FHFPW and RHRPW concepts.

Since the averaging process used to develop Table 17 tended to compress the rankings of the concepts, the relative performance of the scaled facility concepts is more clearly shown by the percentage of the phenomena for which the concept was the highest ranked (or tied for the highest ranked) or the lowest ranked (or tied for the lowest ranked). This information is presented in Table 18 for the Westinghouse type plant and in Table 19 for the Babcock and Wilcox type plant. "Overall Rank" in Tables 18 and 19 is the rank averaged over the important thermal-hydraulic phenomena in all five transients. 1 represents ideal scaling, 4 is unacceptable. "% Highest Ranked" denotes the percentage that the scaled facility concept was the highest ranked or tied for the highest ranking. "% Lowest Ranked" denotes the percentage that the scaled facility concept was the lowest ranked or tied for the lowest ranking and does not include those phenomena for which all the concepts have a rank of unity.

Referring to Table 18 it is shown that for the W plant type the actual FHFPW concept was ranked highest or tied for highest for 68% of the phenomena and lowest or tied for lowest for only 22% of the phenomena. On the other end of the scale the RHRPW concept was top ranked or tied for

TABLE 18. SUMMARY OF FACILITY CONCEPT RANKINGS FOR W PLANT TYPE

<u>Facility</u>	<u>Overall Rank</u>	<u>% Highest Ranked</u>	<u>% Lowest Ranked</u>
FHFPW - Ideal	1.99	57.4	39.5
FHFPW - Actual	1.66	68.1	22.1
RHRPW	2.37	23.4	61.6
RHFPW	1.84	40.4	25.6
LFPW	1.93	47.9	40.7
RHFPP	1.92	41.5	31.4

TABLE 19. SUMMARY OF FACILITY CONCEPT RANKINGS FOR B&W PLANT TYPE

<u>Facility</u>	<u>Overall Rank</u>	<u>% Highest Ranked</u>	<u>% Lowest Ranked</u>
FHFPW - Ideal	2.06	57.7	42.7
FHFPW - Actual	1.72	63.9	23.6
RHRPW	2.35	20.6	56.2
RHFPW	1.86	38.1	23.6
LFPW	1.87	49.5	38.2
RHFPP	1.94	39.2	29.2

top rank for only 23% of the phenomena and ranked lowest or tied for lowest for 62% of the phenomena. Based on the percentage of top rankings or tied for top rankings the ideal FHFPW concept ranked second followed by the LFPW, RHFPP, and RHFPW concepts. The order of the rankings is the same for the B&W plant type as shown in Table 19.

PRACTICAL CONSIDERATIONS

Based on the generalized scaling relationships shown in Table 4 it is obvious that there are endless possibilities for scaled facility configurations. The local phenomena scaling has shown that those concepts which utilize the same fluid and operating conditions in the model as in the plant offer the best representation of the important phenomena. It has also been observed that using like fluids and operating conditions provides for the most direct interpretation of the scaled data with respect to the plant transient. If one agrees with those conclusions then the remaining decisions to be made concerning the selection of a scaled integral facility concept are:

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1. What length ratio should be used?
2. What length ratio to diameter ratio should be used?
3. Is full scaled power required or is scaled decay power sufficient?

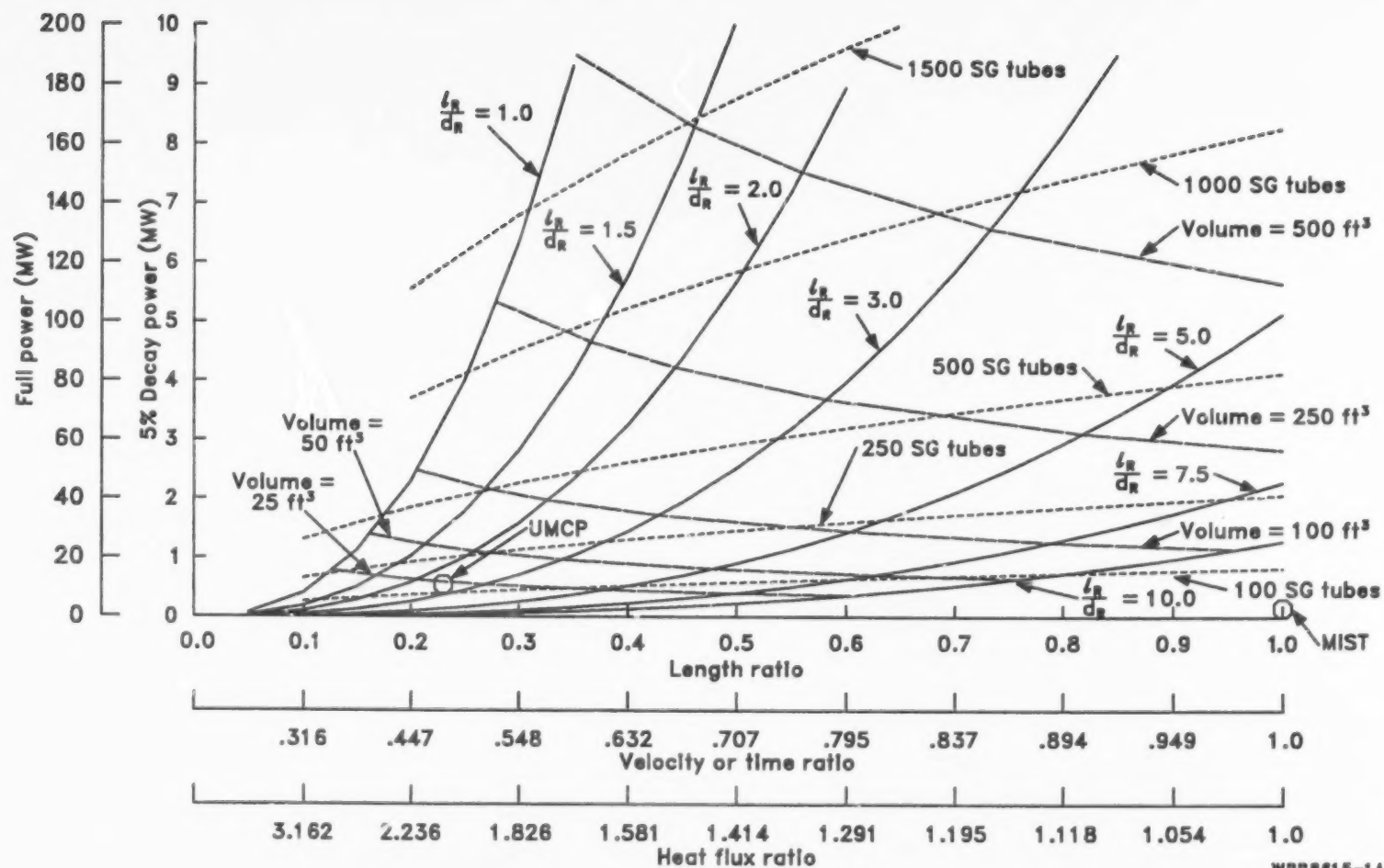
From a strictly technical standpoint the answers to these questions can be found by determining what phenomena for which experimental data are required and then referring to the local phenomena scaling and choosing the concept which provides the best scaling for that phenomena. However, past experience indicates that facility size and consequently facility cost play an important role in the decision as to which scaled facility will ultimately be constructed.

Figure 1 is an attempt to show the trade-off between the various parameters assuming like fluids and operating conditions. This Figure, based on the B&W Oconee PWR, shows the relationship between core power, length ratio, length ratio/diameter ratio, primary system volume, number of steam generator tubes, velocity ratio, time ratio, and heat flux ratio. The linear scaling concept is represented on the far left where the length ratio/diameter ratio is unity while the full height volume scaled concept would be a vertical line on the extreme right above length ratio of unity. It becomes immediately obvious that as linear scaling is approached the system volume and core power requirements become very large except for a very short facility. Assuming that the facility costs will be directly related to facility size it becomes apparent that trade-offs will be required in the area of power and length ratio/diameter ratio for any affordable scaled facility.

CONCLUSIONS

The overall conclusions reached as a result of this study are summarized below:

1. The generalized scaling parameters developed from Ishii scaling were shown to include the specific scaling concepts of linear scaling and full height volume scaling and provide the relationships for using unlike fluids and operating conditions in a scaled integral facility.
2. The actual FHFPW concept was shown to provide the best scaled representation of the largest number of phenomena investigated in the study. This conclusion supports the approach previously adopted by NRC for the design of recent experimental facilities such as MIST and Semiscale.
3. The FHFPW concept is the only concept examined in this study which provides a distortion-free representation of the core and steam generator heat transfer mechanisms. This is a result of the heat flux, velocity, and time ratios being unity.



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Figure 1. Scaled Parameters for Full Pressure Water Scaling of B&W LPWR.

4. Multi-dimensional phenomena in the system components are not well represented in the FHFPW concept. The thermal-hydraulic distortions due to excessive structural mass are greater in the FHFPW concept than in the other concepts investigated.
5. The RHRPW concept was shown to provide the least effective scaled representation of the phenomena investigated in this study. This is primarily a result of property ratios which deviate significantly from unity and vary considerably with pressure.
6. The average rankings for the RHFPW, LFPW, and RHFPF concepts were shown to rank very close together between the FHFPW and the RHRPW concepts.
7. The LFPW concept provides the best representation of multi-dimensional phenomena in the plena and loop components. Horizontal piping flow phenomena including phase separation and counter-current flow limiting are best scaled with the LFPW concept.
8. The RHFPF concept provides a good overall scaled representation of the majority of the phenomena investigated.
9. This study has shown that the scalability of any phenomena for a given scaling concept is basically independent of the plant type investigated. This means that the results of this study can be applied to other thermal-hydraulic systems including advanced light water reactor systems if similar phenomena are involved.

REFERENCES

1. U.S. Nuclear Regulatory Commission, Office of Standards Development, Standard Format and Content of Safety Analysis Reports for Nuclear Power Plants LWR Edition, Regulatory Guide 1.7, Revision 3, November 1978.
2. N. G. Cathey et al., Catalog of PRA Dominant Accident Sequence Information (draft), NUREG/CR-2259, EGG-2259, August 1984.
3. A. M. Kolaczowski et al., Interim Report on Accident Sequence Likelihood Reassessment (Accident Sequence Evaluation Program), Sandia National Laboratory, August 1983.
4. V. H. Ransom et al., RELAP5/MOD-2 Code Manual, Volumes 1 and 2, NUREG/CR-4312, EGG-2396, August 1985.
5. Safety Code Development Group, Los Alamos National Laboratory, TRAC-PF1/MOD-1, An Advanced Best Estimate Computer Program for Pressurized Water Reactor Thermal-Hydraulic Analysis, (draft), NUREG/CR-3858, LA-10157-MS.

6. W. H. Grush and G. E. McCreery, Posttest Analysis of Loss-of-Fluid Tests L3-2 and L3-7, EGG-LOFT-5632, October 1981.
7. T. K. Larson, J. L. Anderson, and D. J. Shimeck, Scaling Criteria and an Assessment of Semiscale Mod-3 Scaling for Small Break Loss-of-Coolant Transients, EGG-SEMI-5121, March 1980.
8. M. Ishii and I. Kataoka, Similarity Analysis and Scaling Criteria for LWRs Under Single-Phase and Two-Phase Natural Circulation, NUREG/CR-3267, ANL-83-32, March 1983.
9. G. Kocamustafaogullari and M. Ishii, Scaling Criteria for Two-Phase Flow Natural and Forced Convection Loop and Their Application to Conceptual 2 x 4 Simulation Loop Design, NUREG/CR-3420, ANL-83-61, May 1983.
10. G. Kocamustafaogullari and M. Ishii, Reduced Pressure and Fluid to Fluid Scaling Laws for Two-Phase Flow Loop, NUREG/CR-4584, ANL-86-19, April 1986.
11. Babcock and Wilcox, Multi-Loop Integral System Test (MIST) Facility Specification, NRC-04-83-168, RP 2399-1.
12. D. W. Sallet et al., "Final Design Report for the UMCP 2 x 4 B&W Simulation Loop," The University of Maryland, College Park, August 10, 1984.
13. T. K. Larson, A Report on Integral System Test (IST) Program Facility Integration, NUREG/CR-4531, EGG ___, (to be published).

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PWR RECOVERY PROCEDURES INVESTIGATED IN THE
LOBI-MOD2 TEST FACILITY

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ABSTRACT

LOBI-MOD2 test results of interest for the analysis of prospected PWR recovery procedures are presented. They relate (1) to the efficiency of the secondary system as an additional heat sink in small break LOCAs, (2) to the recovery of the secondary system heat sink from highly degraded conditions, (3) to potential long term cooldown via primary system bleed and feed and (4) to primary system depressurization using the pressurizer cooling system. The reported experimental results provide a basis for the understanding of underlying phenomena and a reference for the development and the assessment of analytical tools used in PWR safety analysis.

INTRODUCTION

In the event of accident conditions in pressurized water reactors (PWRs), recovery procedures are to be enforced to prevent limiting conditions which could endanger the integrity of the plant. Within this context, the efficiency of the secondary system as an additional heat sink in small break loss-of-coolant accidents (LOCAs) and the governing physical phenomena in prospected recovery procedures from anticipated or abnormal occurrences in PWRs are being investigated in the LOBI-MOD2 test facility.

The LOBI research programme represents an important part of the overall reactor safety research programme of the Commission of the European Communities (CEC). It is being conducted in the Ispra-Establishment of the CEC Joint Research Centre (JRC) and its primary objective is to investigate the thermal-hydraulic response of a 2-loop, 1/700 scaled high pressure integral system test facility / 1 / for a range of conditions relevant to LOCA and Special Transients conditions in PWRs. Clearly, the LOBI-MOD2 test facility is not a demonstration prototype; thus, the acquired experimental results refer specifically to the scaled facility and are not directly extrapolable to full size plants. They provide, however, a basis for the phenomenological understanding as well as for the development and the assessment of system codes used in PWR safety analysis.

The research programme / 2 / which was originally mainly oriented towards the large break LOCA scenario was reoriented following the TMI-2 accident to span the systematic investigation of small break LOCA and Special Transients in PWRs. To date 13 small break loss of coolant experiments covering a wide range of parametric variations, a steam generator performance test, a test facility natural circulation and reflux cooling characterization test and 3 Special Transients tests have been performed with the test facility in the MOD2 configuration.

This paper focusses on the thermal coupling between the primary and secondary system when the latter is used as an additional heat sink in small break LOCAs, and on the governing physical phenomena underlying (1) the recovery of steam generator heat sink from highly degraded conditions, (2) the potential primary system bleed and feed long term cooldown and (3) the recovery of an adequate degree of subcooling via primary system depressurization using the pressurizer cooling system.

SECONDARY SYSTEM COOLDOWN IN LOBI-MOD2 SB-LOCA TESTS

In the event of small break LOCAs in PWRs, the secondary system is generally used as an additional heat sink to guarantee an adequate primary system energy removal and cooldown which cannot always be ensured solely by the break flow. The safety relevance of the secondary

system, especially in the smaller break size range, is underlined by the provision of a controlled primary system depressurization and optimum compensation of the break flow by the safety injection systems.

In the LOBI-MOD2 test facility the primary system was seen to follow closely the secondary system cooldown for the range of break size 1 % and smaller; the break flow and the safety injection systems were sufficient to ensure an effective primary system depressurization and cooldown for small break sizes 2 % and above, Fig. 1 and Fig. 2.

Relative to the high pressure region (>10 bar) and to the smaller break size range (0.4 % to 1 %), energy transport to the secondary system relied mainly on two-phase natural circulation and on reflux condensation within the primary system. For larger break sizes (2 % to 10 %) the primary system depressurized below the secondary system pressure giving rise to potential steam generator reverse heat transfer which, however, was impaired by the early voiding of the primary system SG U-tubes. The time at which the primary system pressure undershoots the secondary system pressure is given in Fig. 3 as a function of break size (the data refer to the cold leg break configuration).

RECOVERY PROCEDURES IN LOBI-MOD2 SPECIAL TRANSIENTS TESTS

Three special transients tests (i.e., A2-90, BT-00 and BT-01) have been performed to date in the framework of the LOBI-MOD2 experimental programme. Each test comprised three phases which were run sequentially although not necessarily related to the progression of the initiating fault. The interest here is on the terminating phases which were aimed at the investigation of the governing physical phenomena during plant recoveries from adverse conditions.

Refill and Cooldown of SG's Secondary Side

The thermal coupling between primary and secondary system during the recovery of steam generator heat sink from highly degraded conditions was investigated in the terminating phase (A2-90-3) of test A2-90. The initiating event (A2-90-1) was a loss of offsite and normal onsite electrical power compounded by the failure of the automatic SCRAM system (LONOP-ATWS). As defined, the test case is equivalent to a station blackout ("SBO")-ATWS event with availability of emergency diesel power. The transient progressed through a second phase (A2-90-2) which featured the boildown of the steam generators secondary side down to about 1 m above the U-tube plate, and through a third phase (A2-90-3) which featured refill and controlled cooldown of the steam generators secondary side.

The physical state of the plant at the start of A2-90-3 was characterized by relatively high primary system pressure (108 bar) and temperature (317°C); the steam generators secondary system was nearly dry. Transient assumptions featured a decay level (2 %) for the core power and, in accordance with the progression of the initiating fault, unavailability of the main coolant pumps was assumed.

The recovery procedure adopted in A2-90-3 consisted of restoring proper primary to secondary thermal coupling by refilling the steam generators secondary side. To ensure adequate primary system heat removal and depressurization a controlled 100 K/h cooldown was imposed on the secondary system, Fig. 4. Heat transport within the primary system was provided by single-phase and two-phase natural circulation, Fig. 5. The balance between core and steam generators energy transfer is given in Fig. 6.

Primary Cooling System Bleed and Feed

The main features of potential long term cooldown via primary system bleed (manual opening of the pressurizer relief valve) and feed (automatic actuation of the high pressure ECC injection system) were investigated in the terminating phase (BT-00-3) of test BT-00.

The initial fault (BT-00-1) was a loss of main feedwater (LOFW) which was in time compounded (BT-00-2) by a loss of auxiliary feedwater (LOAF). At the start of the bleed and feed heat removal process, the primary system was at high pressure (163 bar) and temperature (334°C) and the steam generators secondary water inventories were completely depleted. A core decay heat level and unavailability of the main coolant pumps were assumed during the transient. From pre-test sensitivity studies with RELAP5/MOD1-EUR, the equivalent of three full-size power operated relief valves (PORVs) having a capacity each of 26.5 kg/s of saturated steam at 162 bar, was judged necessary to depressurize the primary system to the HPIS setpoint without excessive loss of primary system fluid inventory. The capacity of 2 HPIS trains of the plant taken as reference for this test, was simulated.

The recovery process was initiated by latching open the experiment PORV. The primary system depressurized quickly to saturation and then continued to decrease at a reduced rate eventually tripping the HPIS flow, Fig. 7. The continuous decrease of primary system pressure caused the HPIS inflow to exceed the PORV outflow thus terminating the depletion of the primary inventory, Fig. 8. As the transient progressed the primary system stabilized at a pressure of about 30 bar. The bleed and feed operating map for test BT-00-3 is given in Fig. 9.

Optimization of Recovery Guidelines in SLB

Accident mitigation procedures based on the optimized recovery guidelines were simulated in the end phase (BT-01-3) of test BT-01.

The initiating event was a small steam line break (SLB) from hot stand-by conditions. The transient progressed through a second phase which aimed at establishing conditions relevant to a pressurized thermal shock (PTS) scenario; that is, high primary system pressure and low downcomer temperature. Primary system depressurization using the pressurizer cooling system (the spray system in a full-size plant is simulated by a bank of cooling coils in LOBI) and in time, unaffected steam generator cooldown constituted the preferred procedure to recover and maintain an adequate degree of subcooling in the primary system.

Relevant initial conditions for test BT-01-3 were a relatively high primary system pressure (107 bar) and high downcomer subcooling (105 K); such a condition violates the critical safety function "Maintenance of Integrity". Transients assumptions consisted in zero core power and running main coolant pumps; the heat sink of the unaffected steam generator was available.

Following actuation of the pressurizer cooling system, the primary system subcooling decreased and eventually an adequate subcooling margin (about 15 K) was attained, Figs. 10 to 12. Thereafter, to satisfy the "Maintenance of Core Cooling", cooldown of the unaffected steam generator at a rate of 100 K/h was initiated. The simulated volume control system, however, was not sufficient to compensate for fluid shrinkage and satisfy the "Maintenance of Inventory". This resulted in a rapid decrease of the pressurizer liquid level and eventually in loss of primary system subcooling. Restoration of adequate pressurizer liquid level and primary system subcooling margin was, however, achieved by cycling the high pressure injection system.

CONCLUDING REMARKS

Relative to the LOBI-MOD2 test facility and to relevant transient assumptions, the following can be drawn from the test cases examined:

- the secondary system is an effective heat sink for primary system energy removal and cooldown for the range of break sizes 1 % and smaller,
- re-establishment of effective primary to secondary thermal coupling from highly degraded conditions and with natural circulation in the primary system was shown,

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- primary system bleed and feed is a viable cooldown procedure provided that the HPIS and PORV flows are optimized,
- recovery of adequate primary system subcooling and proper inventory control may be ensured by the pressurizer cooling system and the safety injection systems.

The LOBI-MOD2 experimental programme is, at present, mainly focussed on the acquisition of experimental data relevant to small break LOCA and Special Transients conditions in PWRs. There are plans to include in such a programme the systematic investigation of prospected emergency operating procedures and the verification/validation of transient management strategies.

REFERENCES

- / 1 / C. Addabbo, L. Piplies, W. L. Riebold:
"LOBI-MOD2: Geometrical Configuration of the Test Facility"
Comm. 4010, CEC-JRC, July 1983
- / 2 / W. L. Riebold:
"LOBI-MOD2 Programme Status and Plans"
Proceedings of the Specialists' Meeting on Small Break LOCA
Analyses in LWRs", Pisa (I), June 1985

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MAIN OBJECTIVES OF THE LOBI RESEARCH PROGRAMME

Investigation of the thermal-hydraulic response of the LOBI integral system test facility for a range of conditions relevant to LOCAs and Special Transients in PWRs

Extension of the experimental data base for the development and the assessment of large system codes used in LWR safety analysis

LOBI EXPERIMENTAL PROGRAMME (status 15.10.86)

	LOBI-MOD1	LOBI-MOD2
LB-LOCA	25	-
SB-LOCA	3*	13**
Special Transients	-	3

* Scoping tests

** Including test facility characterization tests

LOBI-MOD2 TEST FACILITY CHARACTERISTICS

System	loops	2 (1:3)
Volume	total · m ³	0.6
	scale reduction	700
Core	power · MWth	5.28
	length · m	3.9
	no. rods	64
	matrix	8 x 8
	heating	electrical
Downcomer	configuration	annular
	gap width · mm	12
SGs	type	U · tube
	no. tubes: 1-loop	8
	3-loop	24
MCPs	type	centrifugal
	n _q	29.2
Operation	pressure · MPa	15.8
	temperature · C	294/326
Organisation		CEC · JRC
Place		Ispra (Italy)

SAFETY RELEVANCE OF THE SECONDARY SYSTEM IN SB-LOCAs

Guarantees adequate primary system energy removal and cooldown

Ensures adequate primary system fluid inventory by enhancing compensation of the break flow by the safety injection systems

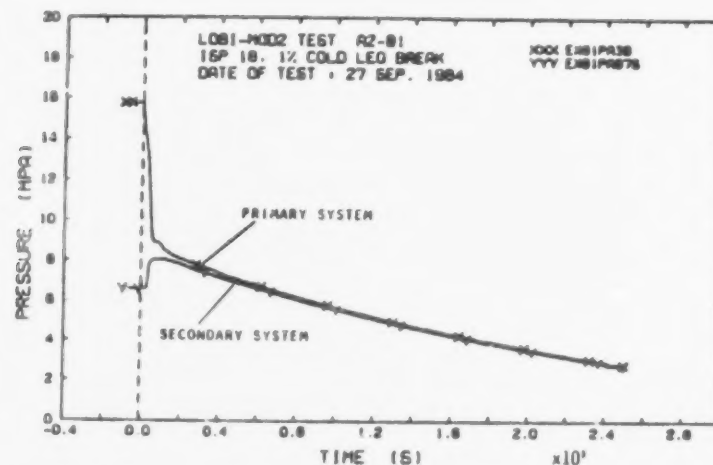


Fig.1: Primary and Secondary System Pressures, 1% CL Break LOCE, SCS CLDWN 100 K/h.

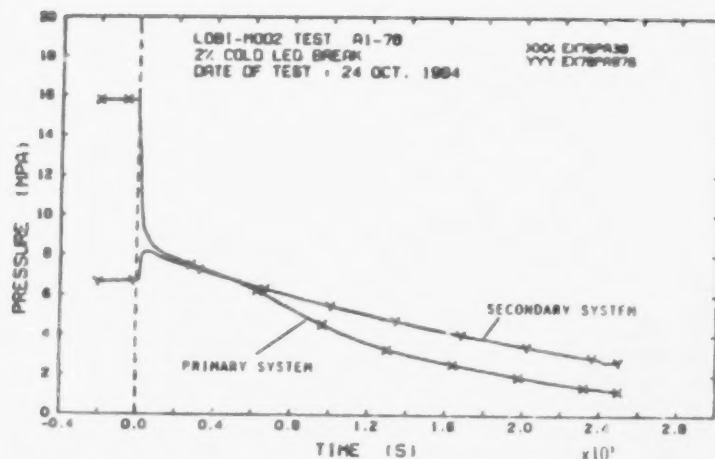


Fig.2: Primary and Secondary System Pressures, 2% CL Break LOCE, SCS CLDWN 100 K/h.

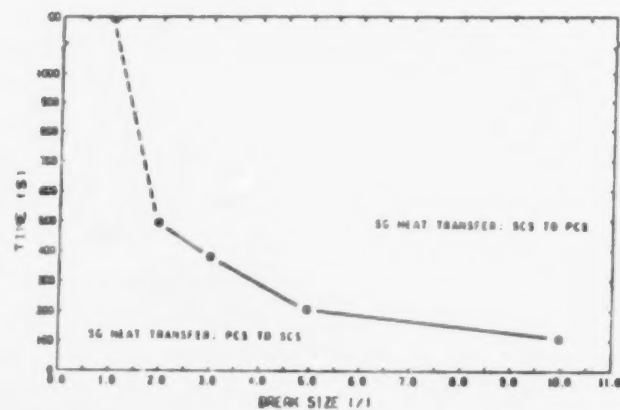


Fig.3: Time at which PCS Pressure undershoots SCS Pressure, LOB1-MOD2 CL Break LOCA Tests.

LOBI-MOD2 TEST A2-90

- 1 LONOP-ATWS
- 2 Boltdown of SG's Secondary Side
- 3 ReHM and Cooldown of SG's Secondary Side

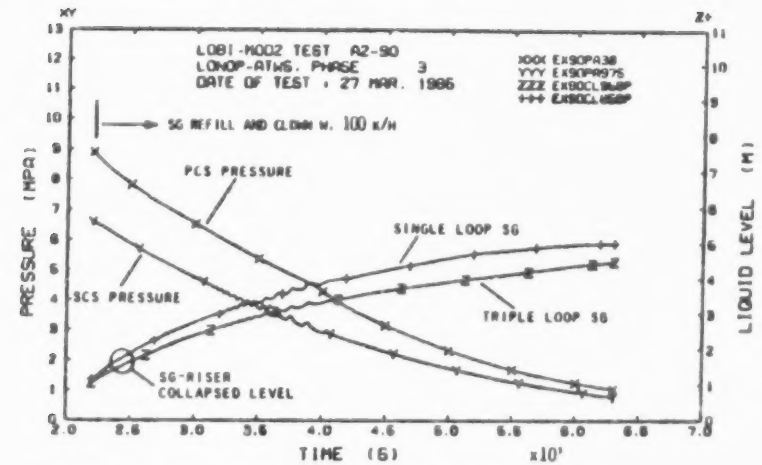


Fig.4: Primary and Secondary System Pressures with Collapsed Levels in Riser of SG Secondaries.

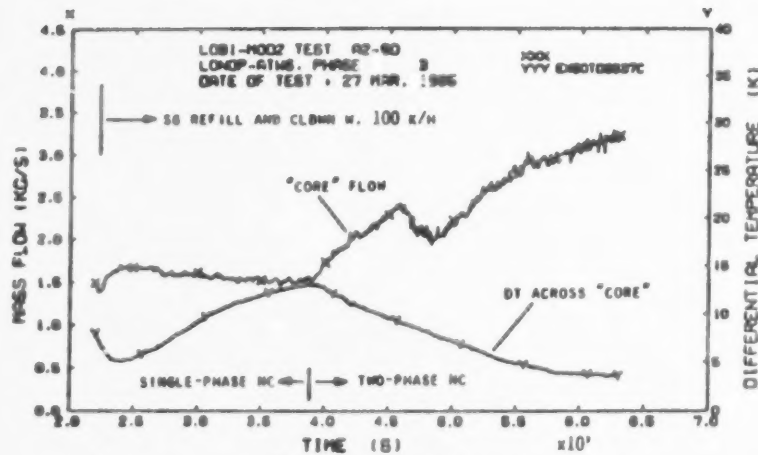


Fig.5: Core Flow and Outlet-Inlet Fluid Temperature Difference.

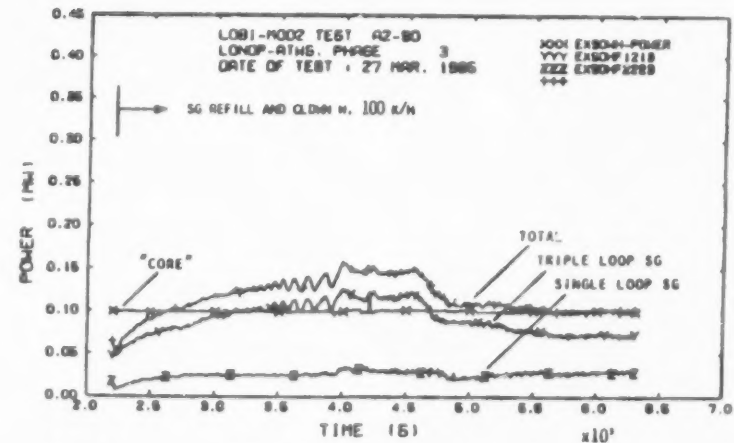


Fig.6: Core and SG Power.

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LOBI-MOD2 TEST BT-00

- 1 LOFW
- 2 LOAF
- 3 PCS Bleed and Feed

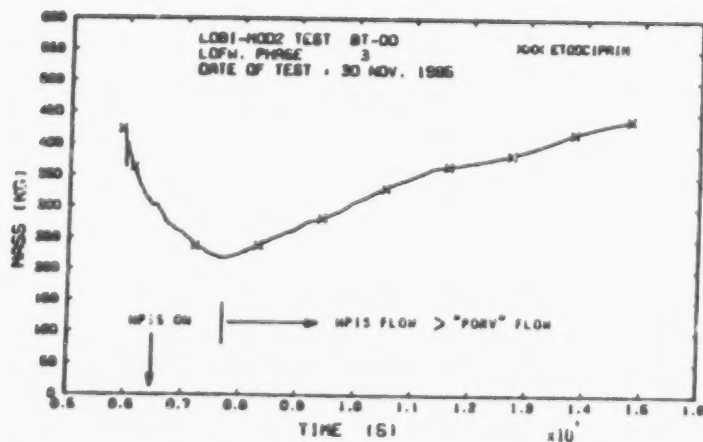


Fig.8: Primary System Mass Inventory.

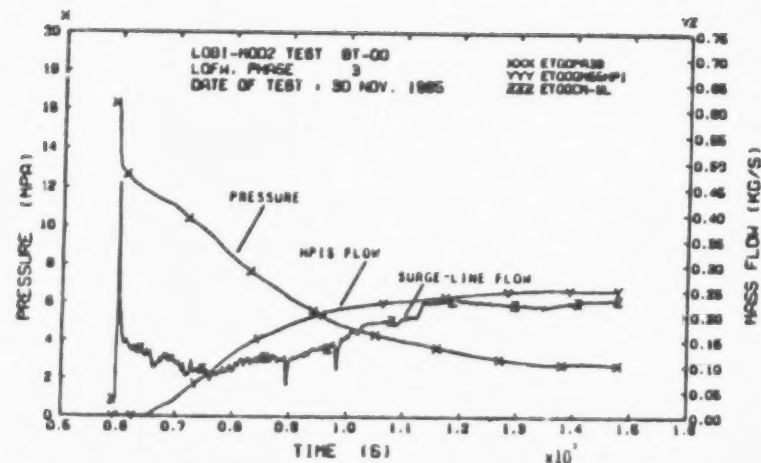


Fig.7: Primary System Pressure with HPIS and Surge-Line Mass Flows.

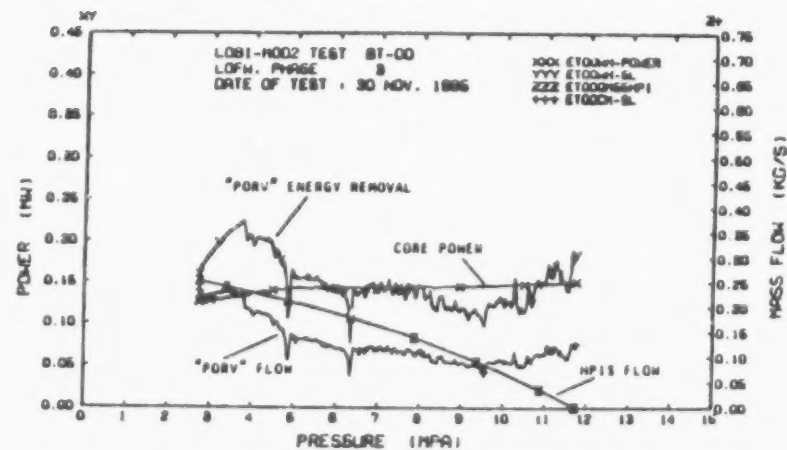


Fig.9: Primary System Bleed and Feed Map for BT-00-3.

LOBI-MOD2 TEST BT-01

- 1 SLB
- 2 PTS Scenario
- 3 Optimization of Recovery Guidelines

* 10%

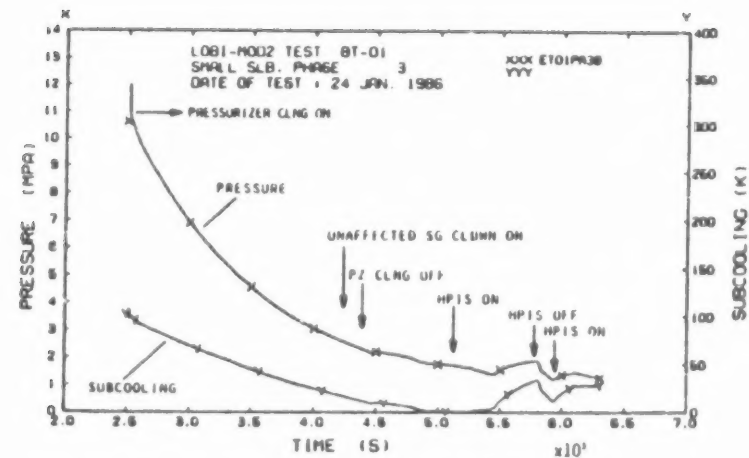


Fig.10: Primary System Pressure and Hot Leg Fluid Subcooling.

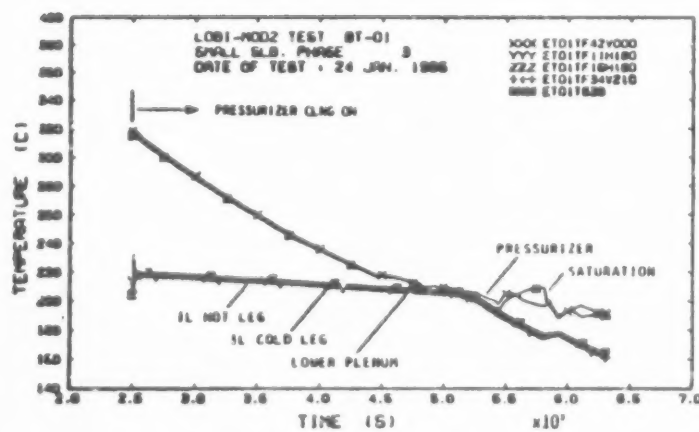


Fig.11: Primary System Fluid Temperatures.

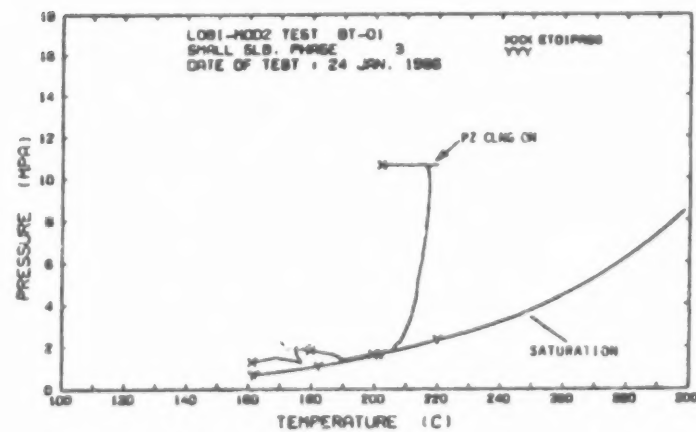


Fig.12: P-T Fluid Diagram in Downcomer Middle Region.

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PKL III SMALL BREAKS AND TRANSIENTS EXPERIMENTAL PROGRAMME

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ABSTRACT

Operational experience as well as risk studies show the probability of a small break being greater by several orders of magnitude than a double-ended break of a primary coolant pipe. Therefore the next test series to be carried out in the PKL* test facility will cover small breaks and operational transients. A scaling and feasibility study was carried out as a result of which changes to the test facility were proposed.

The programme objectives, modification to the facility, scope of tests to be carried out as well as the schedule are described in this article.

INTRODUCTION

During the last decade experimental research concentrated its efforts on loss-of-coolant accidents (LOCA) caused by double-ended breaks of a primary coolant pipe. In Germany the analysis of PKL and UPTF test results shows that containment and consequences of double-ended breaks are largely understood. Operational experience as well as risk studies show the probability of a small break or stuck open valve being greater by several orders of magnitude than a double-ended break of a primary coolant pipe. The next series to be carried out in the PKL test facility, PKL III, will therefore cover small breaks and operational transients.

As the phenomena to be investigated differ from the ones occurring during large break LOCAs, for which the PKL test facility was originally designed, a feasibility and scaling study was carried out to identify those components to which changes must be made.

TEST PROGRAMME OBJECTIVES

The objectives of the PKL III test programm focus on the following areas:

Verification of advanced computer codes

Current computer codes capable of analyzing transients or small breaks have reached relatively high standard. These codes can be verified only by using well instrumented integral test facilities.

* PKL (Primärkreisläufe) test facility is an integral test facility originally designed to carry out loss-of coolant accidents

Investigating optimum shutdown operating procedures following off-normal plant conditions

A well designed test facility is capable of simulating extreme off-design conditions which cannot be reproduced in the real plant. In this way shutdown procedures can be optimized.

Small breaks on the primary or secondary side and non-availability of one or more subsystems fall into this category.

The experience thus gained will also contribute towards simplification and standardization of operating procedures.

Availability of the test facility for investigation of topical nuclear power plant problems

In a country where over 20 nuclear power plants are either on line or under construction, it is in utilities', manufacturers' and public interest to have a well instrumented test facility available at short notice. The time necessary to construct a new facility and train a team to operate it takes several years. Recommissioning of an existing facility and assembling a new competent experimental team cannot be achieved under one year. Continuing the PKL programme will not only produce useful experimental results, it also guarantees permanent availability of a relatively large test facility and of an experienced team.

FEASIBILITY AND SCALING STUDY, FACILITY MODIFICATIONS

The PKL test facility was originally designed as an integral test facility for investigation of the refill and reflood phases of large-break loss-of-coolant accidents (LB LOCA).

As the phenomena to be investigated in the proposed test series differ from those occurring during LB LOCA it was necessary to carry out a feasibility and scaling study to identify those components to which changes must be made in order to fulfil the stated test objectives.

The feasibility and scaling study covered the following areas:

Pressure influence on heat transfer and two-phase flow phenomena, geometry, pressure losses, power, NSSS auxiliary systems, secondary side, instrumentation.

The results are summarized below:

Influence of pressure

The significant two-phase flow phenomena which will be simulated in PKL III take place at pressures between 80 and 2 bar.

An attempt was made to investigate analytically the distortions resulting from the limiting pressure of 40 bar on the PKL primary side i. e. of simulating phenomena at $p = 40$ bar which take place at 80 bar and below.

The pressure determines, above all, the saturation temperature and fluid properties. These in turn influence to greater or lesser extent the driving forces, pressure loss, velocity, heat transfer, onset of countercurrent flow and other quantities.

The density ratio of water and steam is shown in Fig. 1. One can see clearly that the pressure domain 2 - 40 bar essentially covers the whole range.

The influence of pressure on the ratio of pressure losses and driving forces around the loop are shown in Fig. 2, the difference between the two at 40 and 80 bar amounts to about 20 %.

Heat Transfer was investigated for single phase forced convection and natural convection in core and steam generator and for filmwise condensation in steam generator. As the hydraulic diameters in both core and steam generators are identical to those in PWR the only quantities which influence the ΔT s are the fluid properties. Figs. 3, 4, 5, show that the deviations for the three above mentioned cases lie between 10 and 20 %.

The onset of countercurrent flow limitation (CCFL) in steam generator tubes is a function of power. The difference in power required to cause flooding is ca. 1.1 % decay power, Fig. 6, (6.3 % at 40 bar against 7.4 % at 80 bar) i. e. PKL III with its lower pressure will be operating closer to the flooding limit giving more conservative results.

Geometry

The original P.L. power/volume scaling resulting ideally in preservation of real time scaling has been retained i.e.

$$\begin{aligned} \frac{L_{PKL}}{L_{PWR}} &= 1 && \text{flow path length (incl. elevations)} \\ \frac{Q''_{PKL}}{Q''_{PWR}} &= 1 && \text{volumetric heat generation rate} \\ \frac{A_{PKL}}{A_{PWR}} &= \frac{1}{s} && \text{cross sectional area ration where } s = 145 \end{aligned}$$

Certain phenomena, however, cannot be preserved by strict power/volume scaling and here compromises must be sought.

It is a priori impossible to preserve the pressure loss (Δp) and flow path length while reducing only the pipe diameter in a volume scaled facility. The compromise reached for PKL III consisted of:

- preserving flow path lengths, hydraulic diameters and pressure losses in the core, upper plenum and steam generator tubes i. e. in vertical components with the exception of the pump seal and downcomer.
- increasing the diameter and decreasing the flow paths in the hot legs and cross over legs thus keeping the overall Δp around the loops within 5 % of the desired value (ideally $\Delta p_{PKL} = \Delta p_{PWR}$).

The existing already larger-than-scaled diameters of the hot and cold legs were retained and the flow paths were shortened in the ratio $L/\sqrt{D}$ to account for the important phenomenon of flow separation in the hot legs.

The simulation of two or even three loops by a single loop is adequate when modelling LB LOCAs. In tests simulating small breaks or slow transients when compounding failures are to be investigated, a reduction in the number of loops could lead to non-representative test results. Therefore in PKL III all four loops of the prototype plant are simulated, Figs. 7, 8. To preserve symmetry of all four loops the PKL-typical downcomer (upper mixing region annular, lower region two external pipes) will be installed concentrically around the reactor pressure vessel. Fig. 9.

Another difference between LB LOCAs and SB LOCAs consists of simulating the pumps. While in LB LOCAs it is adequate to model the resistance of a coasting or stalled reactor coolant pump (RCP), in SB LOCAs and transients it is imperative to be able to simulate the full range of the pump characteristics. The PKL III pumps will be equipped with variable speed control (0-100 %) and will also be able to reproduce the correct pressure loss when stalled.

Power

The short term power supply of 2.5 MW (10 % of full power) for 30 minutes and 0,8 MW (3.2 %) continuously are adequate for all intended experiments.

Auxiliary Systems

In the course of a transient or SB LOCA a variety of subsystems come into operation. Apart from the low pressure safety injection which already exists the following will be added in PKL III, Fig. 10:

- high pressure injection system (HPIS)
- accumulators
- residual heat removal system
- volume control system
- pressurizer pressure control system (spraying and heating)

Secondary side

The four independent steam generators will include 30 U-tubes each with seven different elevations taking into account the difference of 1.5 m between the U-tubes with the smallest and largest radius, Fig. 11. This difference in elevation influences halting and restarting of natural circulation.

Apart from a few exceptions the secondary side is the only part of the NSSS by means of which the operator can remove energy from the core and thus safely shut down the plant. Great care was taken in simulating the function of all relevant features on the secondary side, Fig. 11, 12. They are:

- scaled volume on the shell side of the steam generators
- downcomer section (partly annular, partly external pipes)
- flow area and resistance of steam driers and separators
- flow limiter.

Outside the steam generators there are four independent

- feed water systems
- emergency feed water systems
- main-steam lines
- blowdown lines

In absence of turbine and condenser the main-steam lines vent to atmosphere.

Heat Losses

The heat losses will be measured during shakedown, at present they can only be estimate using PKL II data. There they amounted to 1.2 % of the available core power (2.5MW). In a test with 2 % decay heat (0.5 MW) the overall heat losses were 6 % of the core power. This is acceptable compared to the value for PWR which is ~2 % (also relative to 2 % decay power). The heat losses in PKL III although somewhat higher because of an additional loop and steam generator, will be of the same order of magnitude as PKL II.

Instrumentation

Only extensive instrumentation of the test facility guarantees optimum use of test results. The 1070 measuring points in PKL III are divided into three categories, Fig. 13:

PKL-test instrumentation	(860)
PWR-identical instrumentation	(60)
PKL-operational instrumentation	(150).

The last will be used to run the facility. The PWR-identical instrumentation has, as its name suggests, identical function and location in PKL as in PWR. After analyzing a test using the extensive PKL instrumentation the PWR-identical instrumentation will be independently evaluated with the aim of finding out whether this information alone would have led the operator to draw the correct conclusions about the phenomena and course of the test.

TEST MATRIX

The first tentative test matrix consisting of six categories is shown in Fig. 14. There are two basic groups of tests:

Transients without breaks and transients with small breaks ($< 10\%$) on the primary or secondary.

The first group, aims at investigating the response of the plant to specific counter measures following, sometimes extreme and improbable, off-design conditions (categories 2,3 and 6).

The second group is concerned mainly with operating procedures and their influence on the course of a test following a small break (categories 4 and 5).

The one year test programme is to consist of approximately 20 tests.

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SCHEDULE

The feasibility and scaling study lasted 6 months and was completed in Dec. 1985. Since Jan. 1986 the test facility is being modified. Shakedown tests are scheduled for March - June 1987 to be followed by a one year period of experiments, Fig. 15.

CONCLUSIONS

The next test series to be carried out in the PKL test facility (PKL III) will cover the area of small breaks and operational transients. The objectives are:

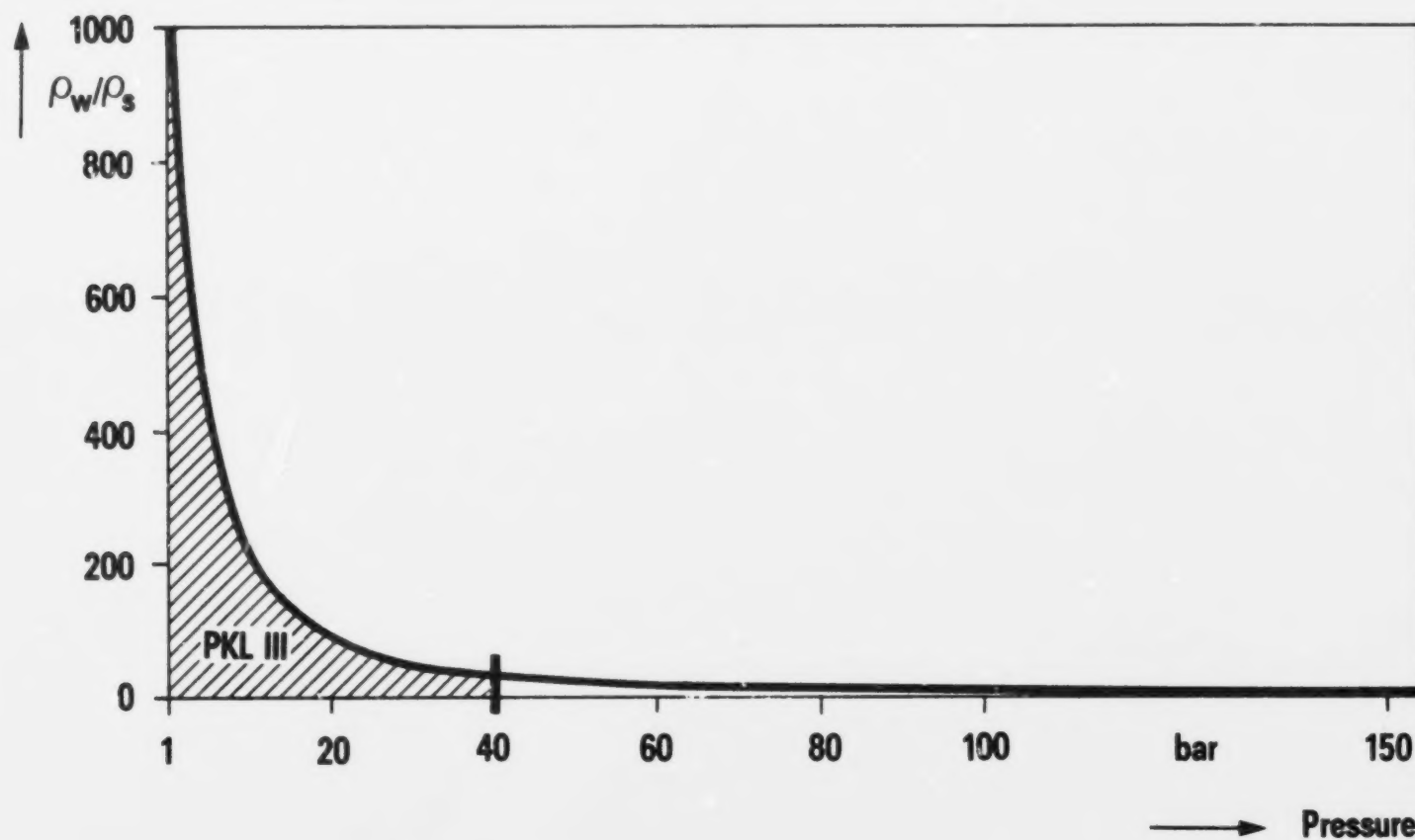
- Verification of advanced computer codes
- Investigating optimum shut down operating procedures following off-normal plant conditions
- Availability of the test facility for investigation of topical nuclear power plant problems.

A feasibility and scaling study was carried out to identify those components to which changes must be made in order to fulfil the stated test objectives. The results of the scaling and feasibility study showed that it is possible to meet the objectives if, in addition to the existing features, the following requirements are met:

- 4 loops
- 4 steam generators
- 4 reactor coolant pumps
- $L/\sqrt{D}$ scaling of loops
- addition of auxiliary systems on both the primary and secondary sides.

The proposed modifications are being carried out in 1986 to be followed by experiments in 1987 and 1988.

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Water to Steam Density Ratio as a Function of Pressure

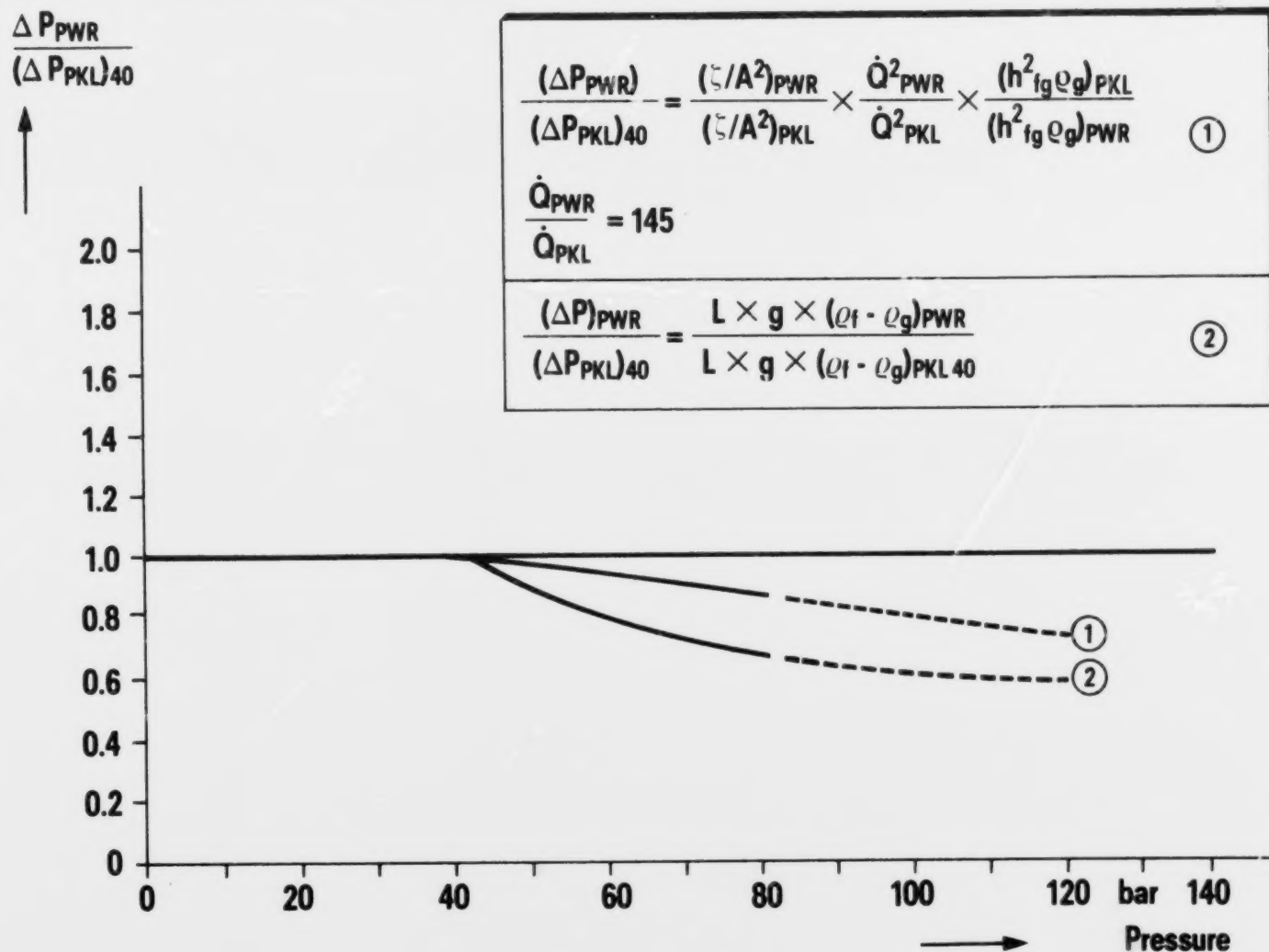
86 PWR 070e

Fig. 1

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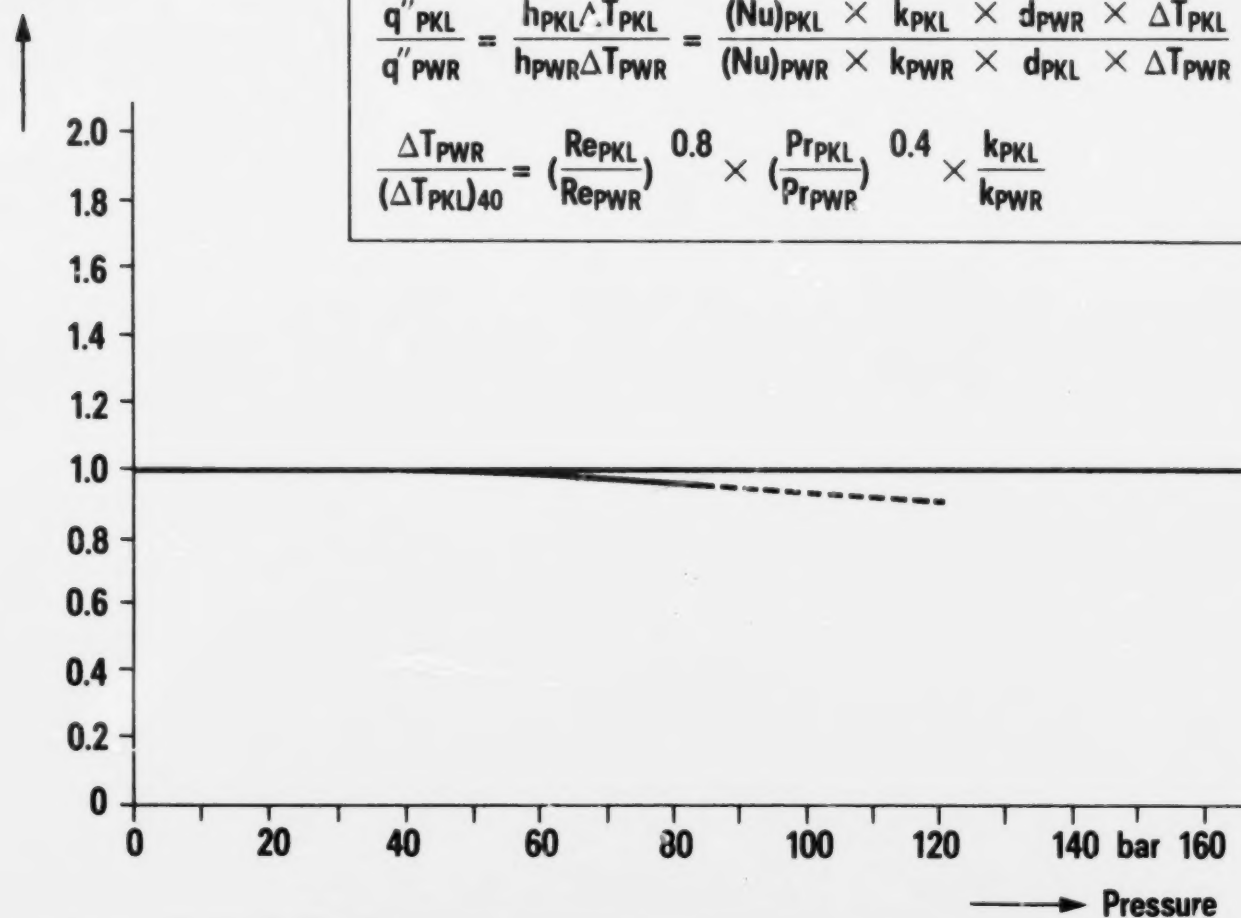


Ratio of Pressure Losses and Driving Forces on Primary Side

86 PWR 052e

Fig. 2

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$$\frac{\Delta T_{PWR}}{(\Delta T_{PKL})_{40}}$$


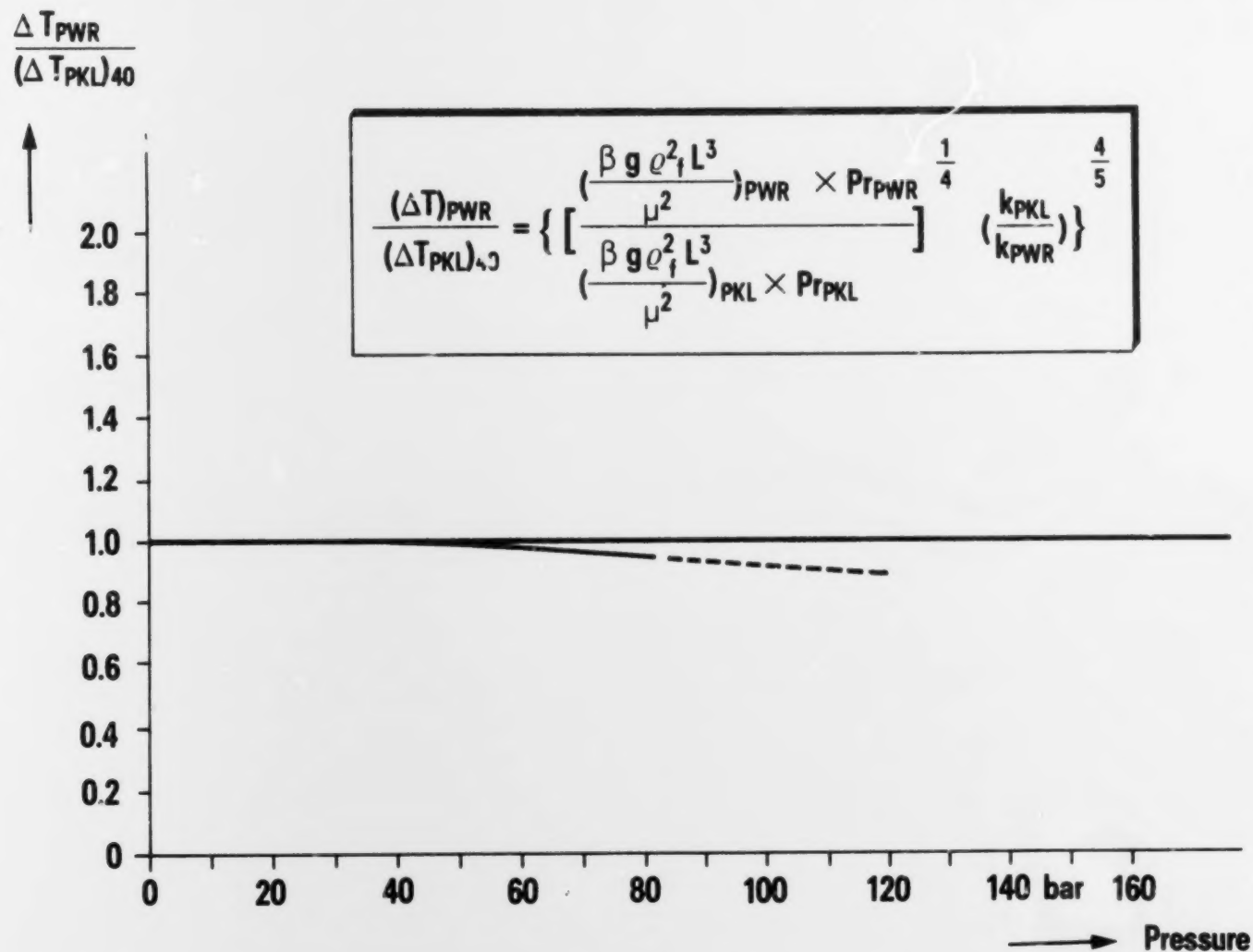
Ratio of ΔT s in Core and Steam Generators.
Single Phase Forced Convection.

Fig. 3 73

86 PWR 051e

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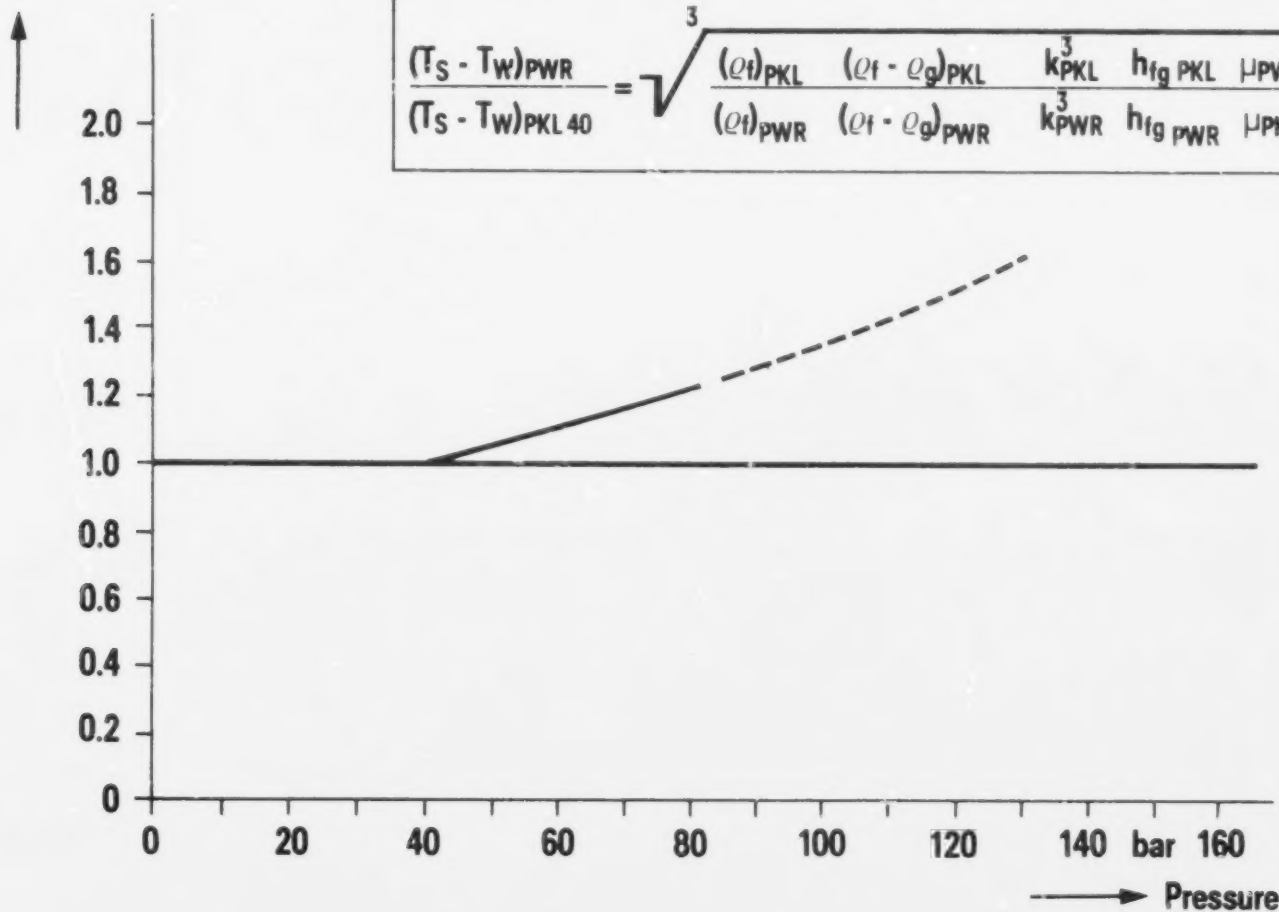


**Ratio of ΔT s in Steam Generators.
Single Phase Natural Convection.**

Fig. 4
74

86 PWR 053e

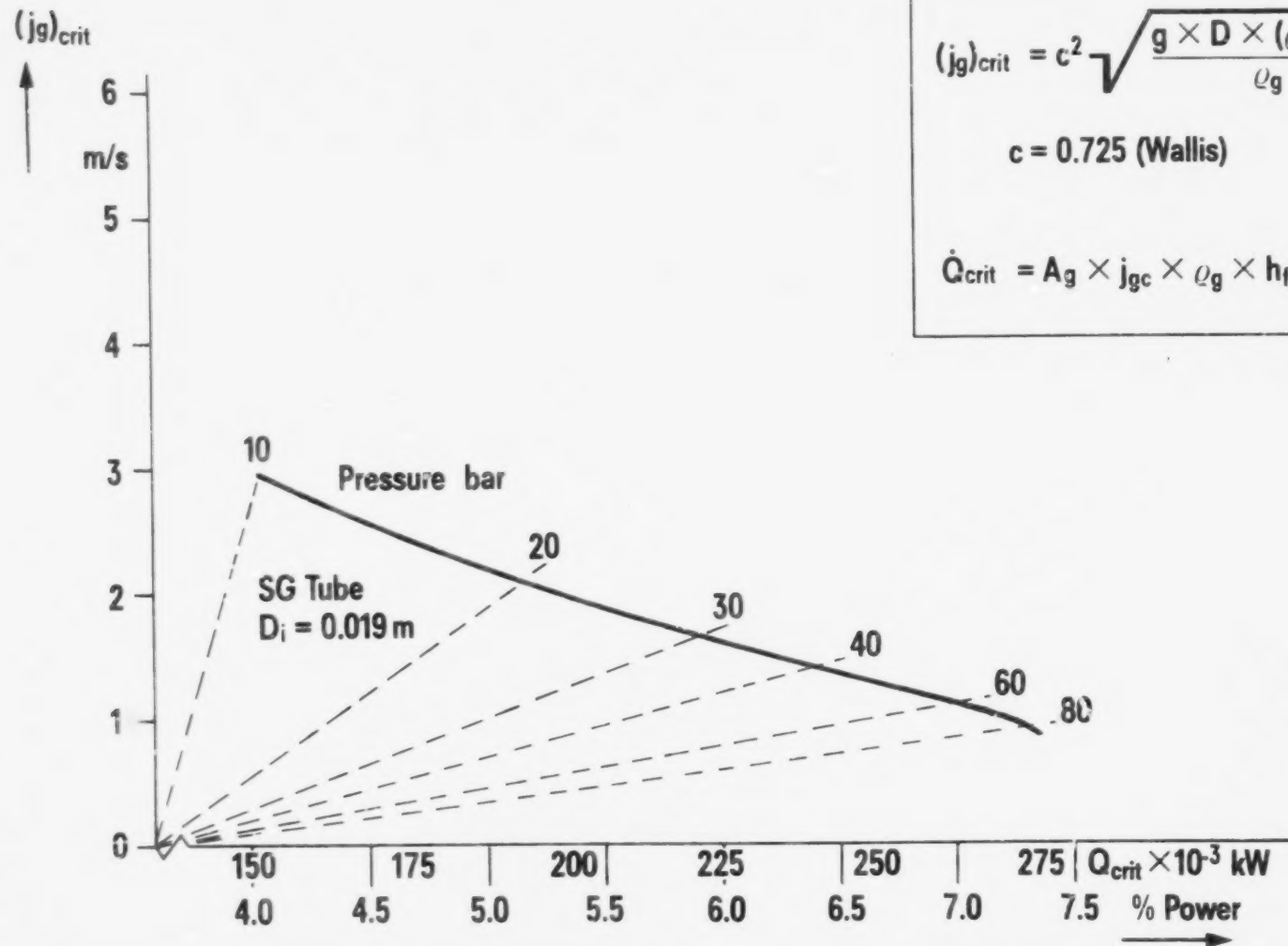
$$\frac{(T_S - T_W)_{PWR}}{(T_S - T_W)_{PKL 40}}$$



$$\frac{(T_S - T_W)_{PWR}}{(T_S - T_W)_{PKL 40}} = \sqrt[3]{\frac{(e_f)_{PKL} (e_f - e_g)_{PKL} k_{PKL}^3 h_{fg PKL} \mu_{PWR}}{(e_f)_{PWR} (e_f - e_g)_{PWR} k_{PWR}^3 h_{fg PWR} \mu_{PKL}}}$$

**Ratio of ΔT_s in Steam Generators.
Filmwise Condensation.**

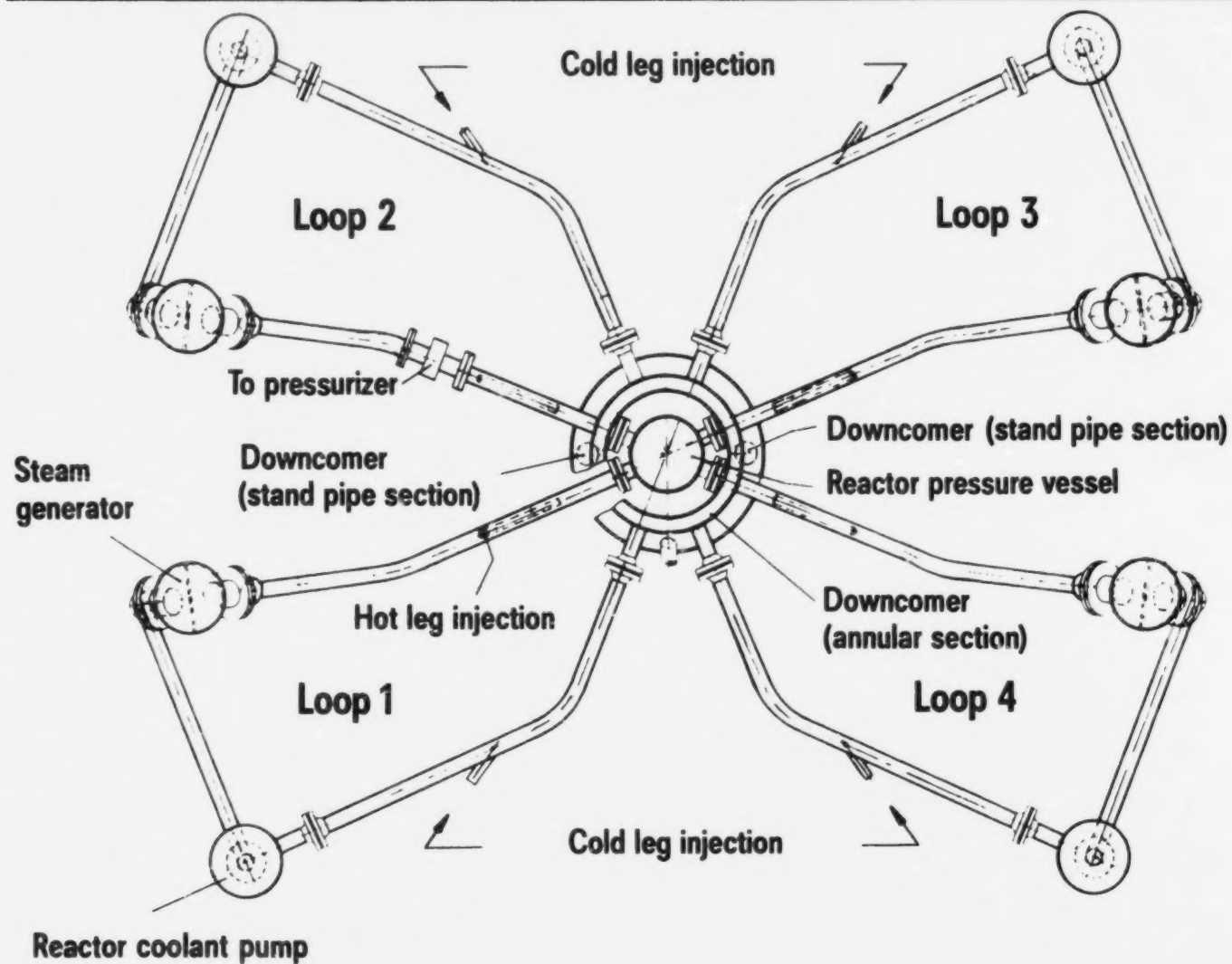
Fig. 5



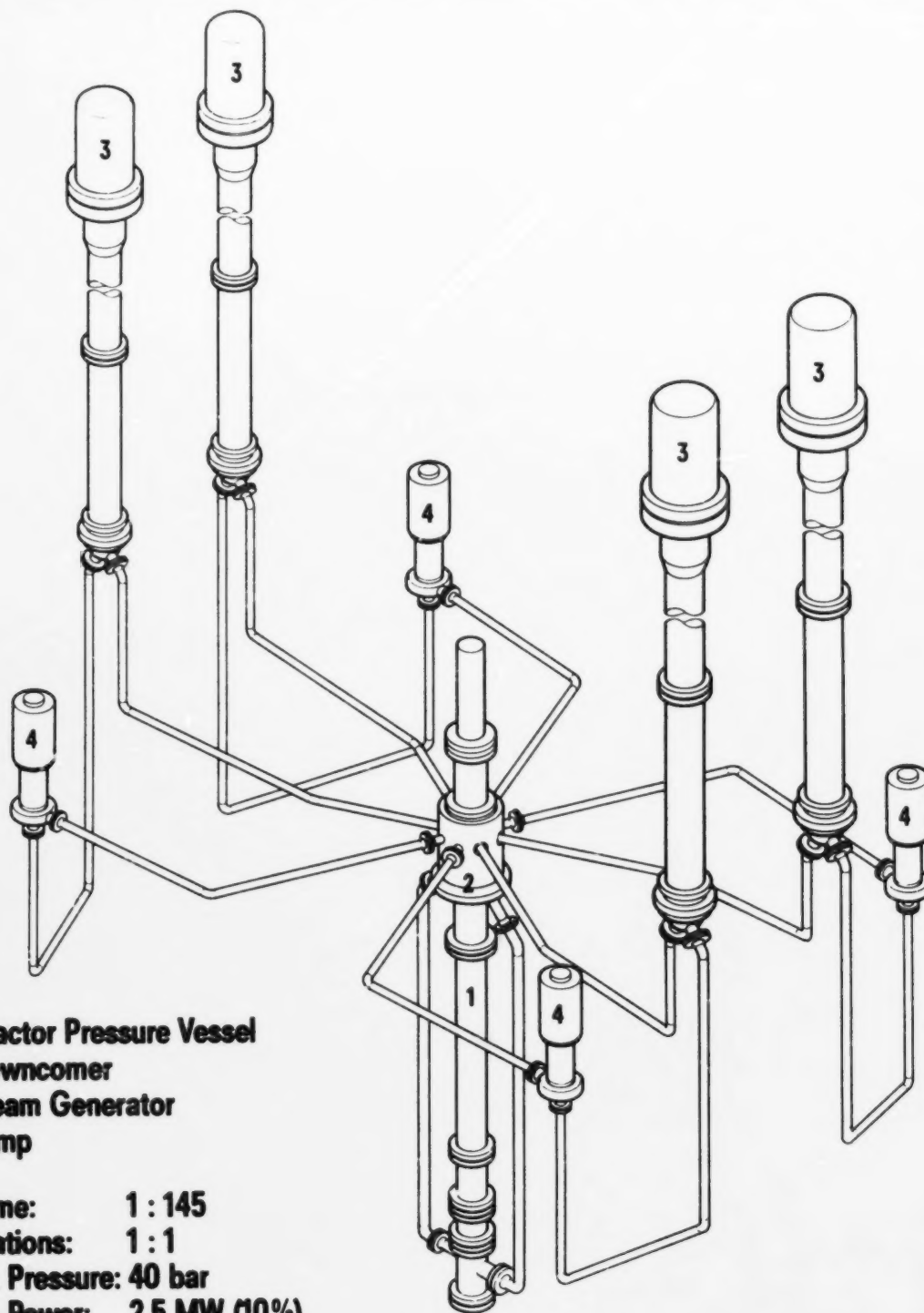
Counter Current Flow Limitation in SG Tubes

86 PWR 057e

Fig. 6



PKL III Plan View
Fig. 7



1 Reactor Pressure Vessel
 2 Downcomer
 3 Steam Generator
 4 Pump

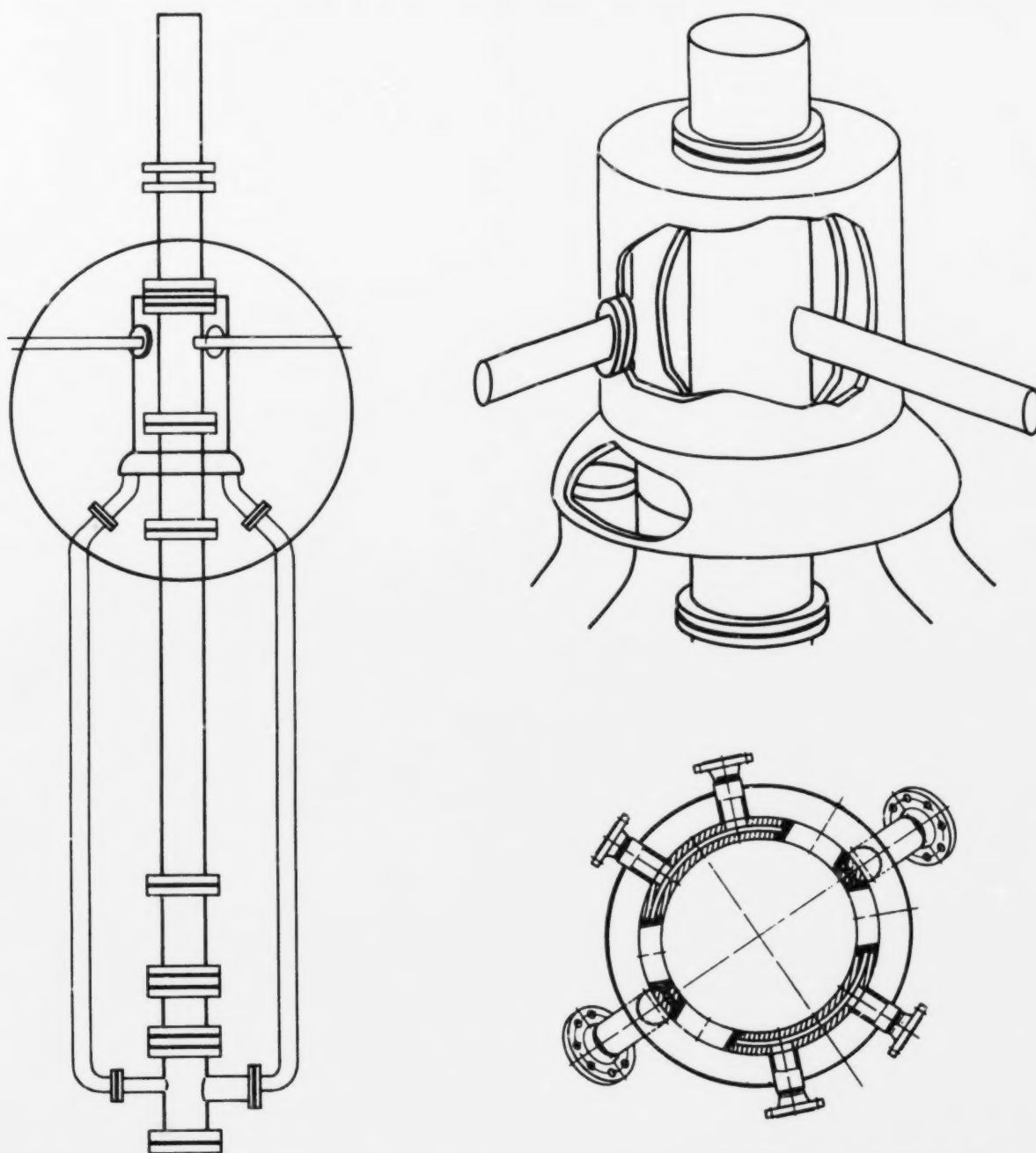
Volume: 1 : 145
 Elevations: 1 : 1
 Max. Pressure: 40 bar
 Max. Power: 2.5 MW (10%)

PKLIII Test Facility Primary Side

86 PWR 061e

Fig. 8

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PKL III Downcomer Design

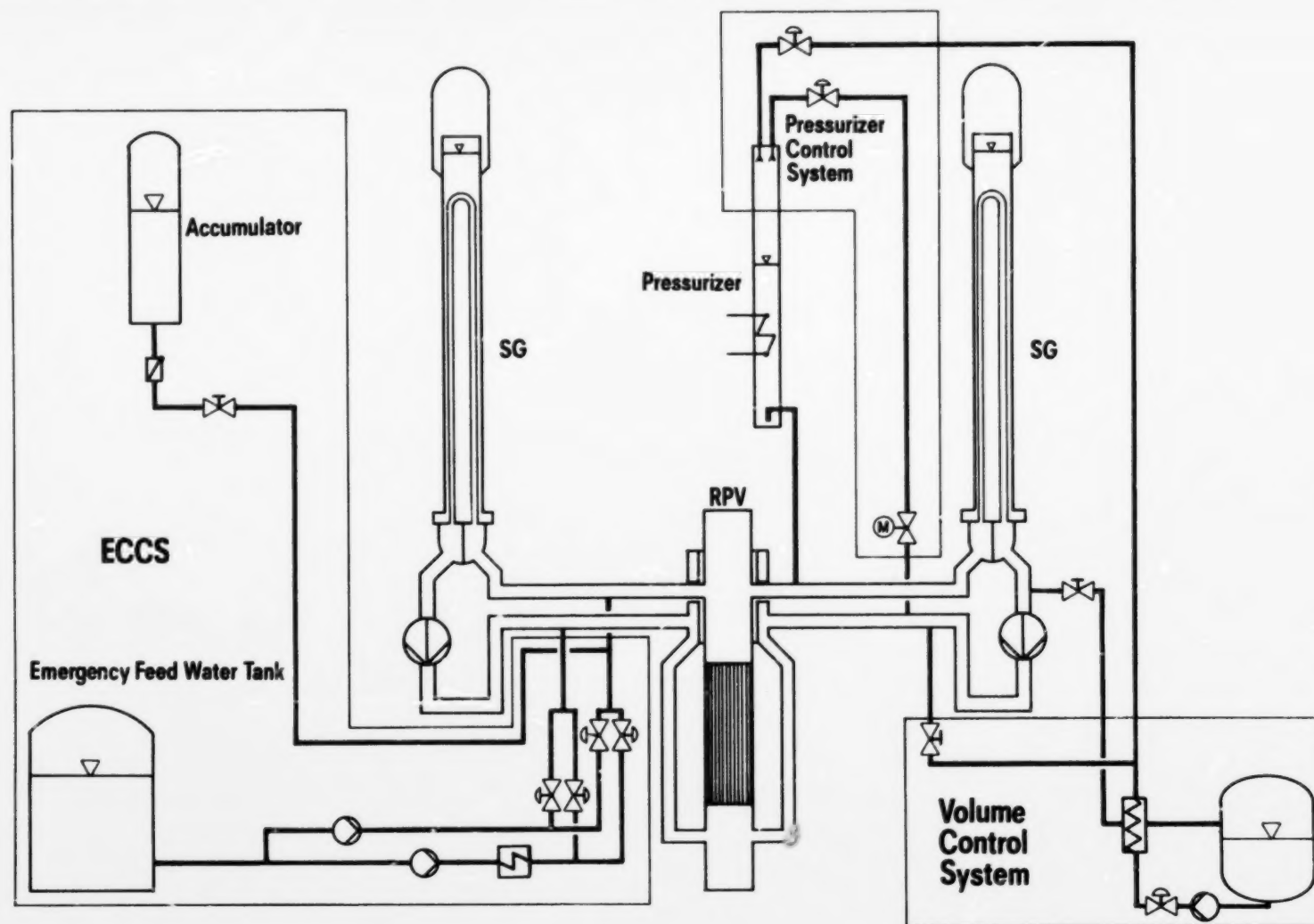
86 PWR 062e

Fig. 9

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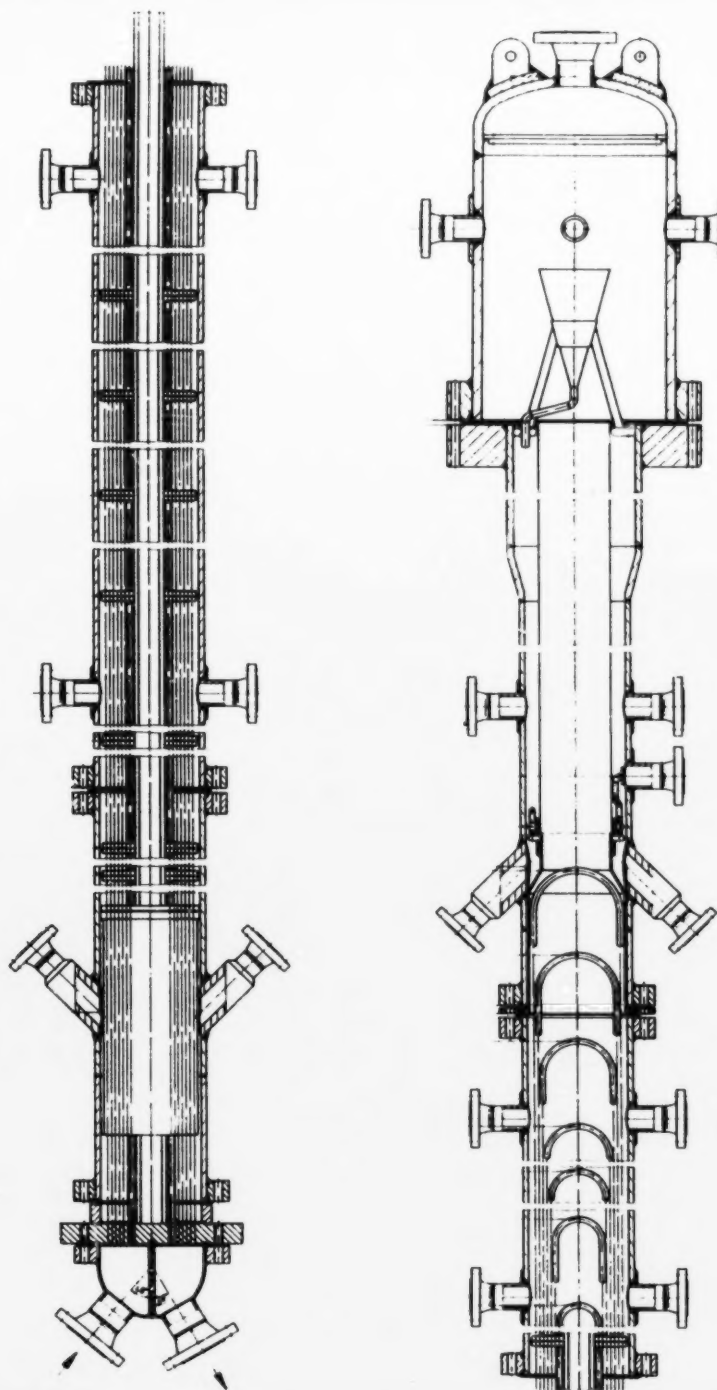


PKL III Engineered Safeguard Systems and
Pressurizer Pressure Control System (2 of 4 Loops shown)

Fig. 10

86 PWR 063e

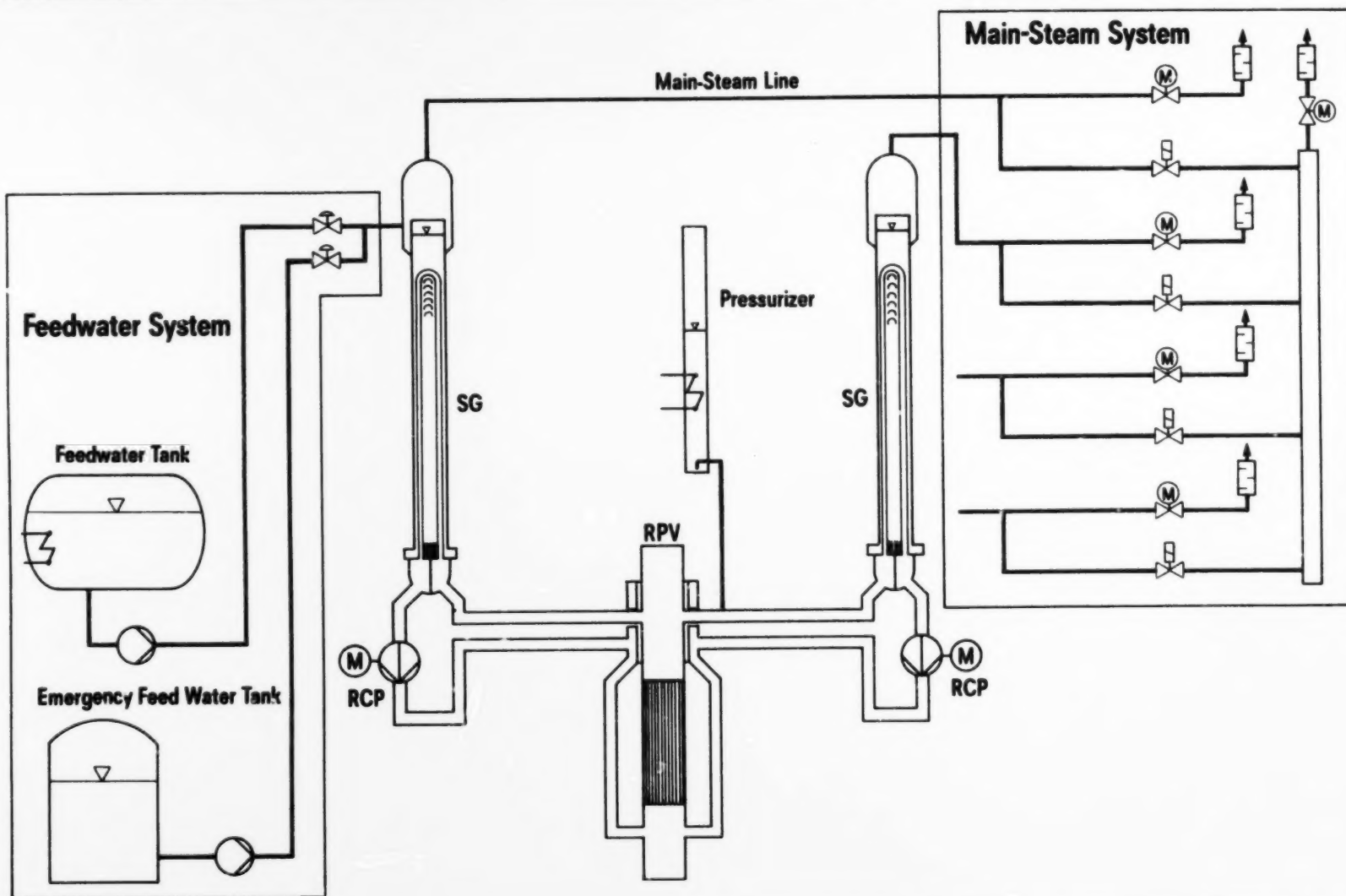
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PKL III - Steam Generator

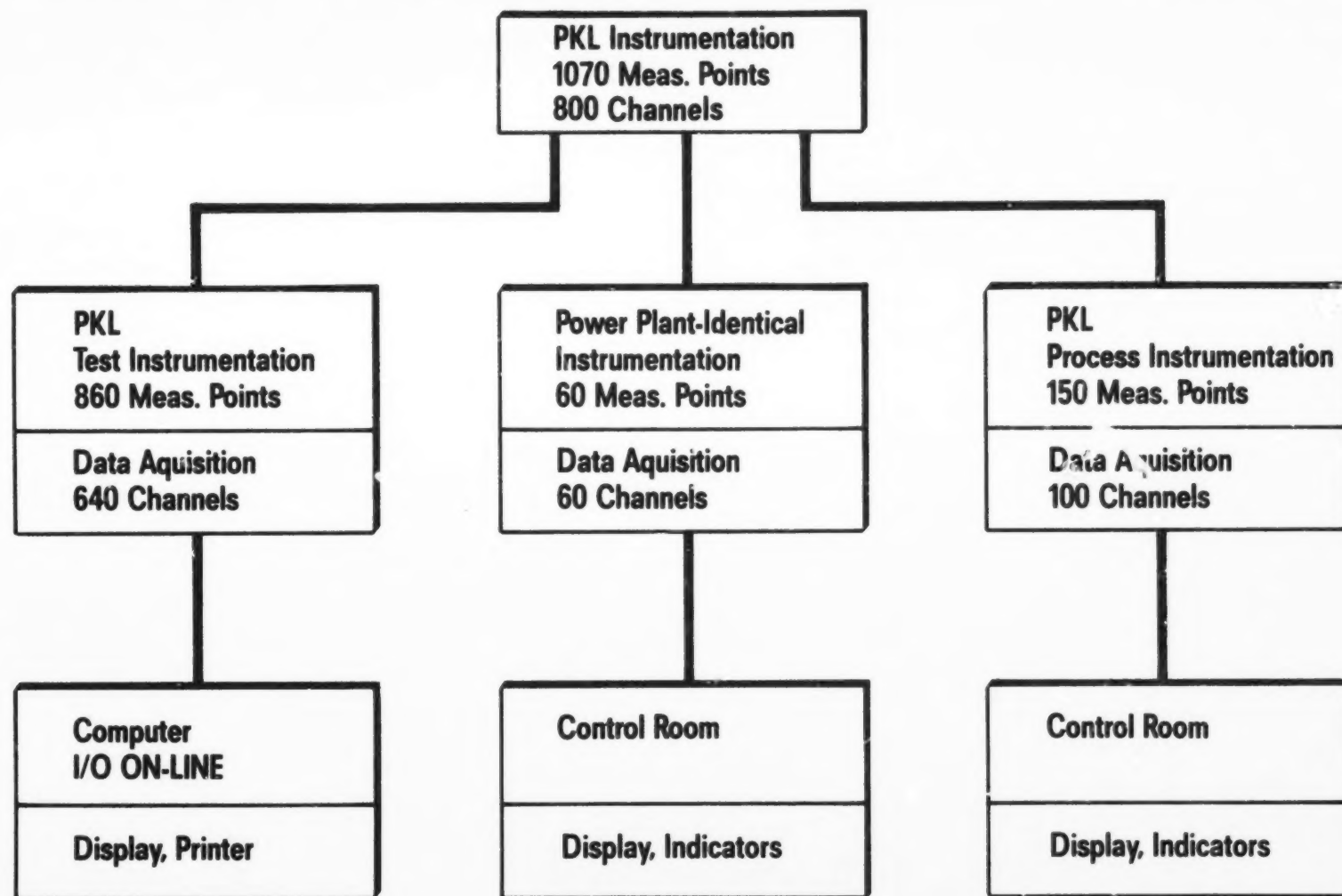
86 PWR 066e

Fig. 11



PKL III Feedwater and Main-Steam Systems
Fig. 12

82



PKL III - Instrumentation and Data Aquisition Concept

Fig. 13

1) Base Line Tests

Shutdown with
4 RCPs and 4 SG operating
1 RCP and 1 SG operating

**4) Small Break on Primary Side.
Operating Procedures**

Break Location:

- Pressurizer
- Hot or cold leg
- SG tube(s)

2) Loss of Off-Site Power

RCPs not in operation,
shutdown with
3, 2 or 1 Steam Generator

5) Break on Secondary Side

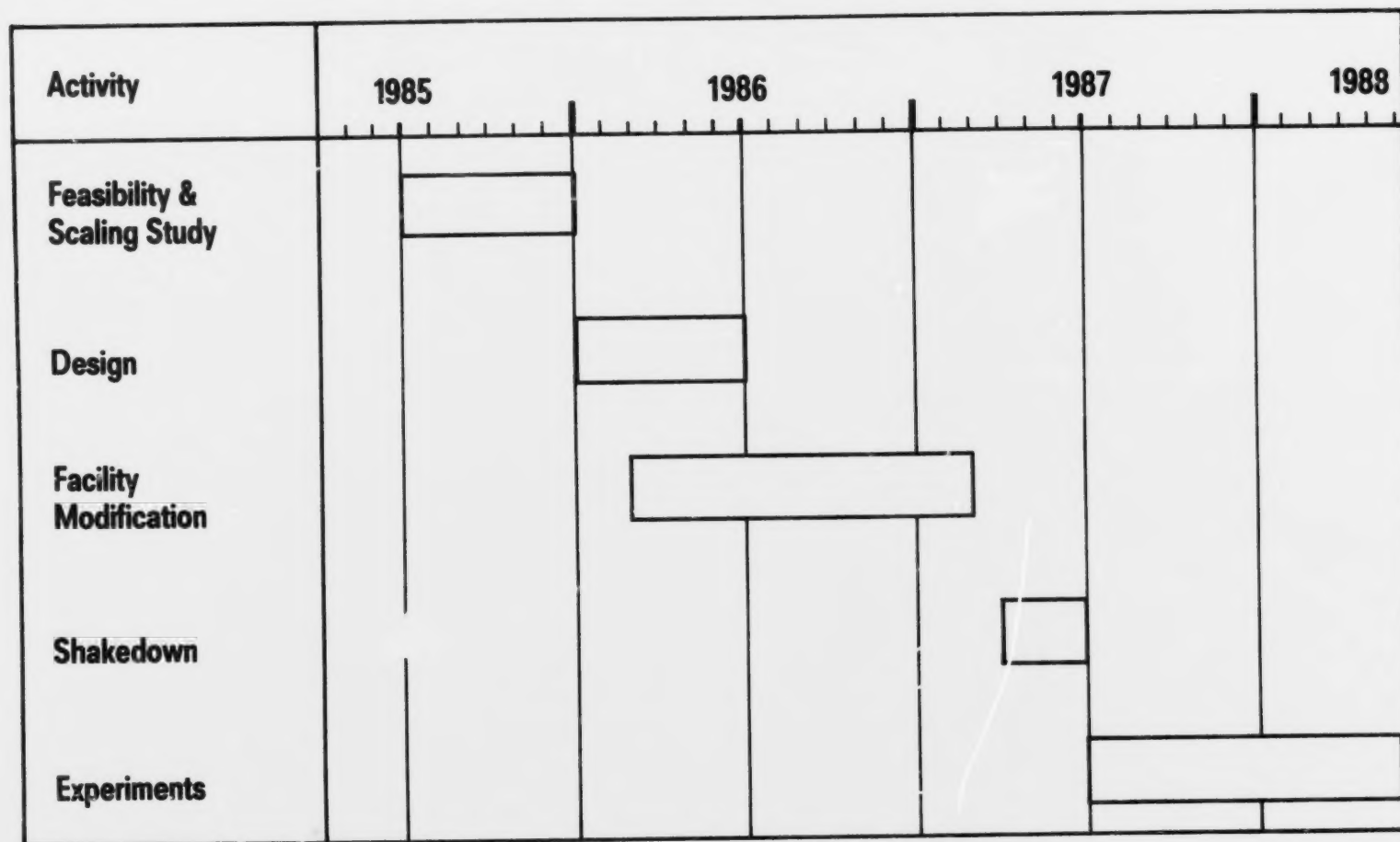
- Main steam line break (200% or less)
- Feedwater line break

3) Criteria for Restarting RCPs

Parameters: state of plant
e.g. RPV bubble

6) Feedwater System Failure

- Delivery of feedwater (hot or cold)
at different water inventories on the secondary
- Delivery of feedwater from intact to
isolated SG via blowdown line



PKL III Schedule (1985 - 1988)

86 PWR 065e

Fig. 15

85

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**SMALL-BREAK LOSS-OF-COOLANT ACCIDENT (SBLOCA) TEST RESULTS
FROM THE MULTILoop INTEGRAL SYSTEM TEST (MIST) FACILITY**

by

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ABSTRACT

The Multiloop Integral System Test (MIST) is part of a multiphase program started in 1983 to address small-break loss-of-coolant accidents (SBLOCAs) specific to Babcock & Wilcox-designed plants. MIST is sponsored by the U. S. Nuclear Regulatory Commission, the Babcock & Wilcox Owners Group, Electric Power Research Institute, and Babcock & Wilcox. The unique features of the Babcock & Wilcox (B&W) design -- specifically the hot leg U-bends and steam generators -- prevented the use of existing integral system data or existing integral system facilities to address the thermal-hydraulic SBLOCA questions. MIST and two other supporting facilities [1] were specifically designed and constructed for this program, and an existing facility -- the Once-Through Integral System (OTIS) (References 2 through 9) -- was also used. Data from MIST and the other facilities will be used to benchmark the adequacy of system codes, such as RELAP-5 and TRAC, for predicting abnormal plant transients. The MIST facility design, transient test matrix, and results from three transient tests are described.

INTRODUCTION

MIST [10] is a scaled 2 by 4 (two hot legs and four cold legs) physical model of a Babcock & Wilcox (B&W), lowered-loop, nuclear steam supply system (NSSS). It was designed to operate at typical plant pressures and temperatures. Experimental data obtained from this facility during post small-break loss-of-coolant accident (SBLOCA) testing are used for computer code benchmarking.

MIST consists of two 19-tube, once-through steam generators; a reactor; a pressurizer; two hot legs; and four cold legs. A scaled reactor coolant pump is present in each of the cold legs. Other loop components include a closed secondary system; four simulated reactor vessel vent valves; pressurizer pilot-operated relief valve (PORV); hot leg and reactor vessel upper head vents; high-pressure injection (HPI); core flood system; and critical flow orifices for scaled leak simulation. Guard heaters, used in conjunction with passive insulation to reduce model heat loss, are included on the steam generator and all primary coolant components.

Approximately 850 instruments are present in MIST and interfaced to a computer-controlled, high-speed, data acquisition system. MIST instrumentation consists of measurements of temperature, pressure, and differential pressure. Fluid level and phase indications are provided by optical view-ports, gamma densitometers, conductivity probes, and differential pressures. Mass flow measurements in the circulation loop are made using venturis and a cooled thermocouple and at the system boundaries using Coriols flowmeters and weigh scales.

The transient test phase is divided into seven groups consisting of a total of 46 tests. The test groups are: Mapping; Boundary Systems; Leak-HPI Configuration; Feed and Bleed; Steam Generator Tube Ruptures; Noncondensable Gas and Venting; and Reactor Coolant Pump Operation. Results from three tests are presented: Boundary System Group, Test 3109AA, 10 cm^2 (0.011 ft^2) Cold Leg Discharge Break; HPI/Leak Variation Group, Test 320101, 5 cm^2 (0.005 ft^2) Cold Leg Discharge Break; and Steam Generator Tube Rupture Group, Single Tube Rupture, Test 340213.

MIST DESIGN

MIST is a scaled (plant power / MIST power = 817), full-pressure, experimental facility arranged to represent the B&W lowered-loop plant design. Like the plant, MIST is a 2 by 4 arrangement with two hot legs and four cold legs, as shown in Figure 1. MIST is designed for prototypical fluid conditions with emphasis on leak tightness and minimization of heat loss.

Scaling priorities for MIST were elevation, post-SBLOCA phenomenon, component and piping volumes, and irrecoverable pressure losses. MIST is full elevation throughout. The only elevations compromised are: the top of the pressurizer; the top plenum of the reactor vessel; the inlet and outlet of the steam gener-

ator plenums; and several stagnant fluid zones. Key interfaces are maintained -- these include: the hot leg, U-bend spillover; upper and lower tubesheets of the steam generator (secondary faces); cold leg low point; pump discharge; cold and hot leg nozzles; core (throughout); and points of ECCS (emergency core cooling system) injection.

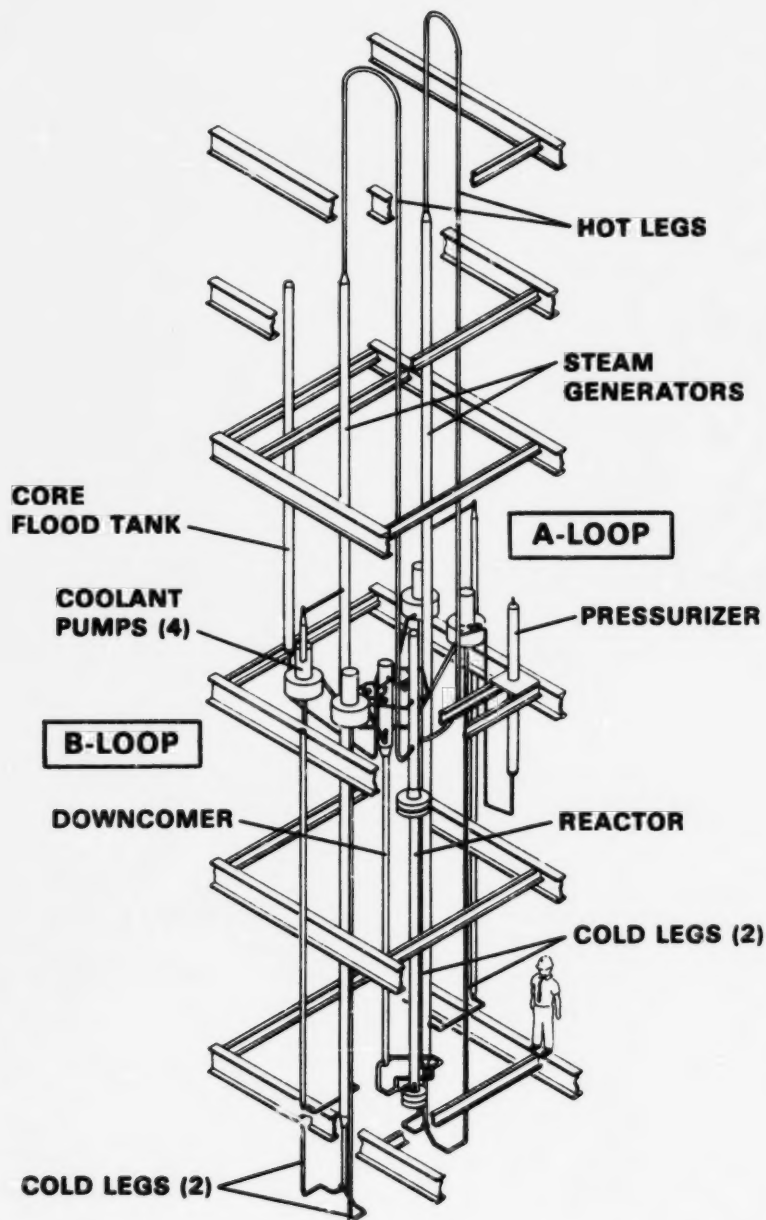


Figure 1. Reactor coolant system -- Multiloop Integral System Test (MIST).

Two-phase behavior during voiding of the hot leg U-bend and flow interruption is sufficiently prototypical; that is, both the plant and the model will experience phase separation early in the post-SBLOCA transient. Hot leg pipes in MIST are large enough to admit bubbly flow.

Fluid volume is 40% larger than ideal volume scaling would dictate. The hot legs, cold legs, and upper downcomer are oversized. This atypicality is imposed by the previously described two-phase characteristics and by preserving component irrecoverable pressure losses.

COMPONENTS

The MIST core and steam generators are full-length subsections of their plant counterparts. The core consists of a 7 x 7 array of 45 full-length, 0.430-inch diameter heater rods and four in-core guide tubes. Plant-typical fuel pin pitch and grid geometry are used. A fixed, axial heat flux profile and a flat, radial heat flux profile are used.

The steam generators each contain 19 full-length tubes. The tubing diameter (5/8-inch OD), material, and tube bundle's triangular pitch (7/8 inch tube centerline-to-centerline) are prototypical. The geometry of the tube support plate (TSP) is similar to the plant and provides equivalent characteristics of irrecoverable pressure loss.

The hot legs use 2.5-inch diameter Schedule 80 piping (2.32-inch ID). The hot leg U-bend maintains the pipe diameter, and a 1.61-foot bend radius is used to conform to the model system layout. The elevation of the hot U-bend spillover is prototypical.

The four cold legs preserve elevation throughout. Schedule 80 piping (2-inch diameter, 1.939-inch ID) is used primarily to match irrecoverable pressure drop. This piping size also preserves cold leg Froude number, which governs the mixing of HPI and reactor vessel vent valve fluid streams. The cold leg horizontal piping runs are shortened, but the slope of the plant cold leg discharge piping is approximately maintained. HPI is injected into the sloping pipes at the appropriate elevation, and the diameter of the model HPI nozzle is selected to preserve the ratio of fluid momentum between the cold leg and HPI.

A model reactor coolant pump is mounted in each cold leg. Suction and discharge orientations are prototypical. The pumps deliver single-phase scaled flows at plant typical heads, allow for simulated pump bumps by matching the plant pump spinup and coastdown times, and permit operation under single- and two-phase conditions. However, the pumps do not preserve specific speed or the two-phase degradation characteristics of the plant pumps.

The MIST reactor vessel employs an external annular downcomer. Cold leg coupling is restricted by using fins in the downcomer annulus to form quadrants. Each downcomer quadrant is connected to a separate RVVV simulation and cold leg. The two nozzles on the core flood tank are connected at the interface between two downcomer quadrants.

The geometry of the model downcomer is annular down to the elevation of the top of the core. Just above the top of the core, the downcomer is gradually reconfigured to form a single pipe for the remaining elevation. The lower downcomer region obtains roughly power-scaled fluid volume over the elevation of the core. Four model RVVVs are used to simulate eight plant valves [11].

The MIST pressurizer is volume scaled and contains heaters and spray. The lower pressurizer elevations are prototypical, as are those of the surge line. The model pressurizer height is reduced from that of the plant to increase the diameter. This lessens atypical fluid stratification and the likelihood of spray impinging on the vessel wall.

One core flood tank is used in MIST. This tank is volume scaled to represent two plant tanks. The model tank is installed vertically with the bottom of the tank at the prototypical elevation. The injection line from the tank to the nozzle on the downcomer is sized to preserve plant-typical irrecoverable losses, and the nozzle is sized to maintain the plant ratio of core flood injected fluid momentum to the downcomer fluid momentum.

BOUNDARY SYSTEMS

The MIST boundary systems are sized to power scale the plant boundary conditions. HPI and auxiliary feedwater (AFW) head-flow characteristics are based on composite plant characteristics. Scaled model vents are included in each hot leg, the pressurizer, and reactor vessel upper head. Leaks are located in the cold leg suction and discharge piping, and the upper and lower elevations of the Steam Generator B (for tube rupture simulation). The desired vent and leak flows are obtained using power-scaled restrictors.

HEAT LOSSES AND GUARD HEATERS

MIST is designed to minimize heat losses from the reactor coolant system. Fin effects (instrument penetrations through the insulation) are minimized by using 1/4-inch penetrations for most of the instrumentation. Heat losses due to conduction through component supports are minimized by designing the supports to reduce the cross-sectional area and placing insulating blocks between load-bearing surfaces. The reactor coolant system piping and vessels are covered with passive insulation, active insulation (or guard heaters), and an outer-sealed jacket (to prevent chimney effects). The guard heaters are divided into 42 zones; each is controlled by a zonal temperature difference and pipe metal temperature.

INSTRUMENTATION

Approximately 850 instruments are present in MIST and interfaced to a computer-controlled, high-speed data acquisition system. MIST instrumentation consists of measurements of temperature, pressure, and differential pressure. Fluid level and phase indications are provided by optical viewports, conductivity probes [8], differential pressures, and gamma densitometers. Mass flow measurements at the system boundaries are made using Corioles flowmeters and weigh scales. Mass flow rate measurements in the loop are performed with

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venturis or turbines. Tables 1 and 2 provide a summary of the MIST instrumentation by component location and instrument type.

Table 1

MIST INSTRUMENTATION BY COMPONENT LOCATION

<u>Component</u>	<u>Number of Instruments</u>
Cold Legs	164
Core Flood	7
Hot Legs	121
Pressurizer	25
Primary Boundary Systems	72
Reactor Vessel and Core	169
Steam Generators	249
Steam Generator Feedwater and Steam Circuit	<u>44</u>
TOTAL	851

Table 2

MIST INSTRUMENTATION BY MEASUREMENT TYPE

<u>Component</u>	<u>Number of Instruments</u>
Conductivity Probes	36
Cooled Thermocouple	12
Differential Pressure	133
Differential Temperature	42
Fluid Temperature	381
Gamma Densitometer	12
Limit Switches	79
Mass Flow	9
Metal Temperature	69
Miscellaneous	17
Power	48
Pressure	9
Volumetric Flow	<u>4</u>
TOTAL	851

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TRANSIENT TEST PROGRAM

The MIST transient tests are defined to generate integral system data for code benchmarking. The transient test series is divided into seven groups:

- Mapping
- Boundary Systems
- Leak-HPI Configuration
- Feed and Bleed
- Steam Generator Tube Rupture
- Noncondensable Gas (NCG) and Venting
- Reactor Coolant Pump (RCP) Operation

At the time this paper was written, the first five groups (through steam generator tube rupture) had been performed.

The 10 planned mapping tests were performed to examine the initial post-SBLOCA transient interactions. In these tests, the primary system inventory was carefully controlled and slowly varied to provide careful examination of the normally rapid and overlapping post-SBLOCA events.

The boundary system tests examined the adequacy and impact of the major boundary system simulations of MIST; namely, the RVVVs, guard heating, level controls of the steam generators, and AFW wetting. These tests were the first to be conducted after mapping to ensure that the boundary system simulations were understood and that the proper simulation had been selected before most of the transient tests were performed. One test with a plant-versed operator was also scheduled early to permit revision of the subsequent tests, if required. The base test conditions for the group included: a scaled, 10 cm², cold leg discharge leak; full HPI and AFW available; no NCG; RCPs not available; automatic RVVV actuation on differential pressure; automatic guard heater control; constant steam generator level control (after refill); and symmetric steam generator cooldown. The intertest variations included were: RVVVs manually closed; RVVVs manually opened; no guard heating; band control of the secondary steam generator level; asymmetric steam generator cooldown; and use of abnormal transient operating guidelines (ATOG). A repeat of the base test was also performed.

Leak sizes, locations, and HPI capacity were varied in the leak-HPI group. Two tests were conducted with changes in leak size. Leak sizes of 5 cm² and 50 cm² were performed with the cold leg discharge leak location. Other tests with varied leak location were performed -- one with a cold leg suction and a second with a PORV leak location. The remaining two tests were a break isolation and a reduced HPI capacity test.

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The feed and bleed cooling tests examined HPI-PORV cooling. In each test, a complete loss of feedwater was simulated and no leaks were used. The feed and bleed test used full HPI, which is activated when the PORV lifted. No venting was performed during the test transient.

The steam generator tube rupture (SGTR) tests study system interactions resulting from single and multiple leaks in steam generator tubes. Tests were performed with simulated breaks of 10 tubes at the top and bottom of the steam generator, single tube leaks at the top of the steam generator, a steam line break superimposed on a rupture of 10 tubes, and a 10-tube rupture followed by isolation on the affected steam generator.

Six noncondensable gas (NCG) and venting tests are defined. These tests examine the impact of hot leg and reactor vessel vents and NCGs on the post-SBLOCA interactions. One test is defined to determine the maximum amount of NCG that can be tolerated before encountering a facility limit, such as maximum primary pressure. This amount of NCG will be used in three subsequent tests without any vents, with hot leg vents, and with reactor vessel vents, respectively. One test will be performed without NCG but with hot leg vents in use. Helium will be used for all tests requiring NCG, except one test that will use nitrogen.

The reactor coolant pump (RCP) group includes six tests that will be performed after the primary loop is reconfigured to install the turbines for forced flow measurement. One test is a repeat of a boundary system test to assess the effects of loop reconfiguration. Two additional tests are repeats of the feed and bleed and SGTR tests, but with the RCPs operating. Two of the tests examine the effect of a continuously running pump and a pump stop with minimum loop inventory on a post-SBLOCA transient. The remaining test is performed at the direction of a plant-versed operator.

TEST RESULTS

The transient tests in MIST were started in June 1986. At this writing (September 1986), 32 of the 46 tests had been completed. Results for one test from each of the boundary system, leak/HPI variations, and steam generator tube rupture groups are presented.

BOUNDARY SYSTEM, 10 cm^2 , COLD LEG DISCHARGE BREAK

The facility was initialized in steady-state natural circulation for the 10 cm^2 (0.011 ft^2) cold leg discharge break test (Test 3109AA). A scaled core power of 3.9% was used and primary pressure was adjusted to obtain a core exit subcooling of $22 \pm 2^\circ\text{F}$. The secondary side of the steam generators was controlled to a 1010 ± 10 psia steam pressure, the level was maintained to 5 ± 1 feet, and auxiliary feedwater was injected at $110 \pm 20^\circ\text{F}$. The initial conditions are summarized in Table 3.

Table 3

INITIAL CONDITIONS FOR BOUNDARY SYSTEM, 10 cm², COLD LEG DISCHARGE BREAK
(Test 3109AA)

<u>System</u>	<u>Parameter</u>	<u>Initial Condition</u>
PRIMARY	Pressure	1739 psia
	Core exit subcooling	23.9°F
	Core power	128.5 kW (3.9% of full-scale power)
	Pressurizer level	23.2 ft
SECONDARY	Pressure	
	Generator A	1014 psia
	Generator B	1015 psia
	Level	
	Generator A	4.6 ft
	Generator B	4.7 ft
	Feedwater Temperature	
	Generator A	121.6°F
	Generator B	122.8°F

The test was initiated by opening a scaled 10 cm² (0.011 ft²) leak in the cold leg piping downstream of the B1 reactor coolant pump. (Note that the leak opening time = 0 on referenced plots.) When the pressurizer level reached approximately 19 feet, 130 seconds after leak opening, the facility operators performed five initiation actions. These were to:

- Start steam generator secondary level fill to 31.6 feet with auxiliary feedwater injected to each generator according to the head/flow curve shown in Figure 2.
- Initiate HPI to each cold leg with the total head/flow characteristics noted in Figure 3.
- Start the simulated core power decay curve noted in Figure 4.
- Switch the reactor vessel vent valves to automatic control (open/close based on differential pressure between reactor vessel and downcomer).
- Initiate the steam pressure cooldown controls.

During the course of the transient, the secondary steam generator level was maintained at approximately 31.6 feet. HPI was maintained on the head/flow curve of Figure 3, unless the core exit subcooling exceeded 70°F. (Throttling and/or termination of HPI was performed when the core exit subcooling exceeded 70°F.) The steam pressure cooldown controls were automatic controls patterned after the Abnormal Transient Operating Guidelines (ATOG) to maintain a positive (approximately 50°F) primary-to-secondary (core exit to steam generator maximum saturation) temperature difference.

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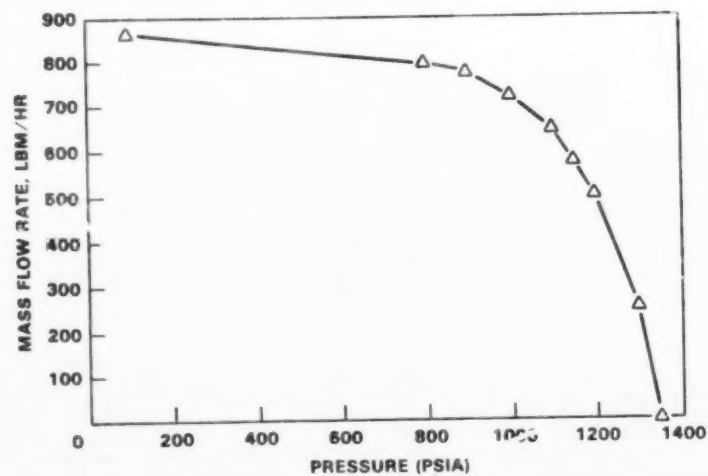


Figure 2. Auxiliary feedwater head/flow characteristics.

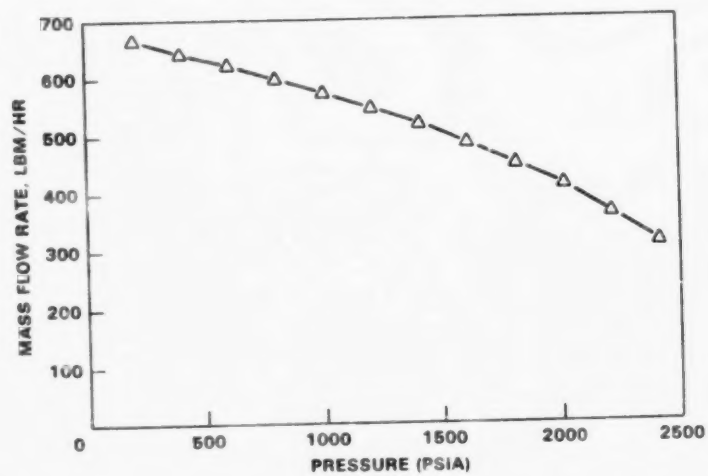


Figure 3. HPI head/flow characteristics.

FROM 3.5% (WITH 13 KW ADDED FOR LOSSES TO AMBIENT)

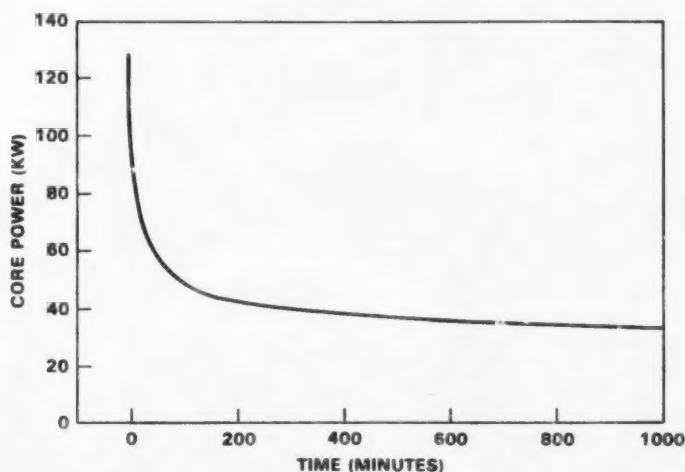


Figure 4. Simulated core power decay.

At 2.4 minutes after leak opening, the A-loop (the pressurizer is connected to the A-loop hot leg) hot leg U-bend saturated. Shortly thereafter, the A-loop hot leg levels began to recede from the U-bend spillover elevation (Figure 5), the A-loop flow abruptly decreased (Figure 6), and the secondary of Steam Generator A depressurized due to continuing feed flow to reach the 31.6-foot secondary level control elevation (Figure 7). The Steam Generator A primary level descended to the elevation of the upper steam generator tubesheet at roughly 1 minute before the completion of secondary side refill at approximately 9 minutes (Figure 8), triggering the condensation of primary vapor near the auxiliary feedwater (AFW) injection site (auxiliary feedwater-boiler condenser mode, "AFW-BCM").

In Steam Generator A, the activity lessened the differences in fluid conditions between the A and B loops. Consequently, the B-loop levels receded from the hot leg U-bend spillover elevation while the A-loop levels increased from 13 to 25 minutes (Figure 8). The realignment of the hot leg levels interrupted primary-to-secondary heat transfer and caused the primary system pressure to remain near 1100 psia from 16 to 25 minutes (Figure 7). Repressurizations were inhibited by intermittent B-loop flow. The interruption of primary-to-secondary heat transfer became sustained beyond 27 minutes. The primary system depressurized only gradually in response to the excess of leak (volumetric) flow rate over HPI flow.

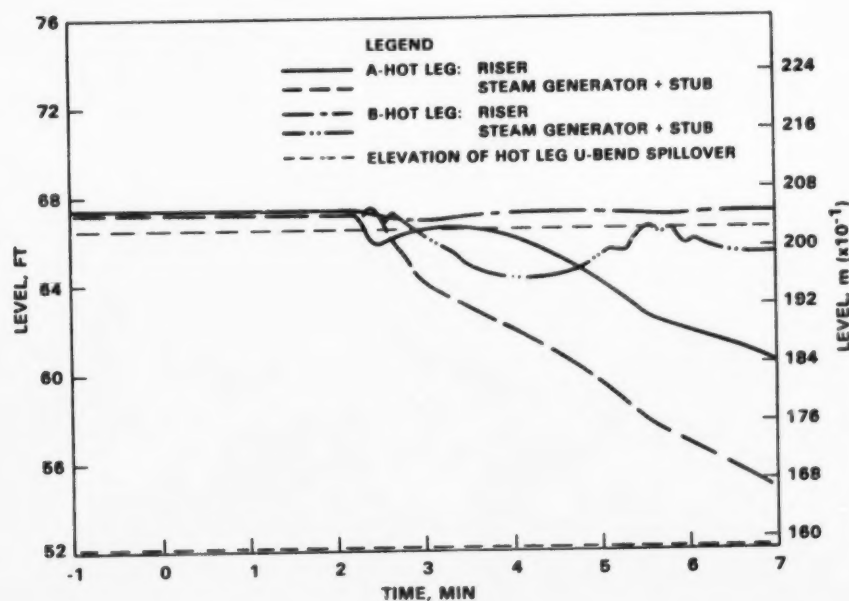


Figure 5. Hot leg riser and stub collapsed liquid levels (Test 3109AA).

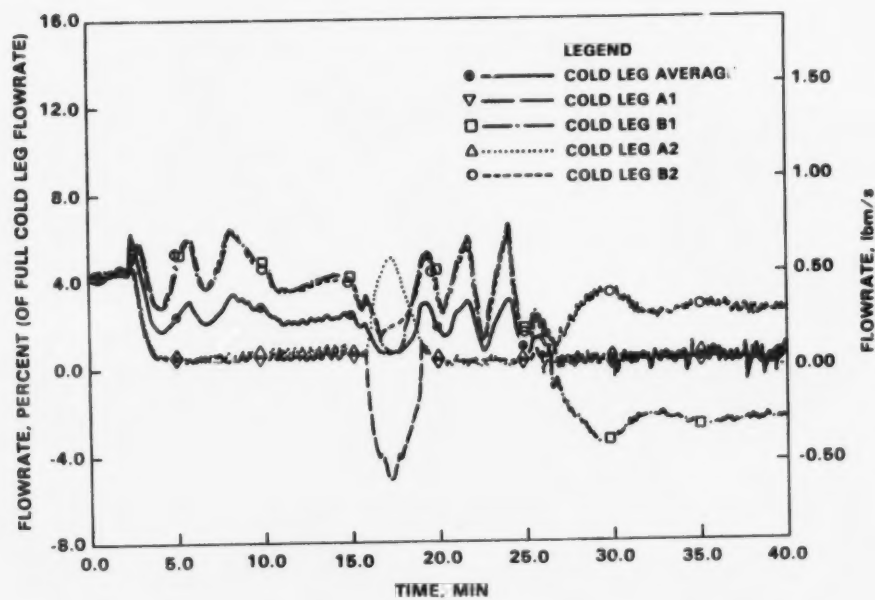


Figure 6. Cold leg flow rates (Test 3109AA).

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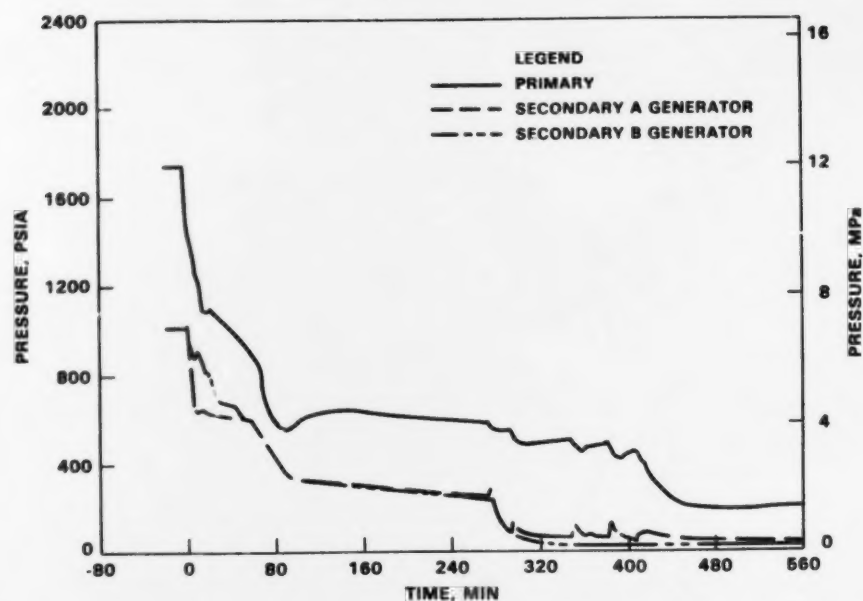


Figure 7. Primary and secondary pressures (Test 3109AA).

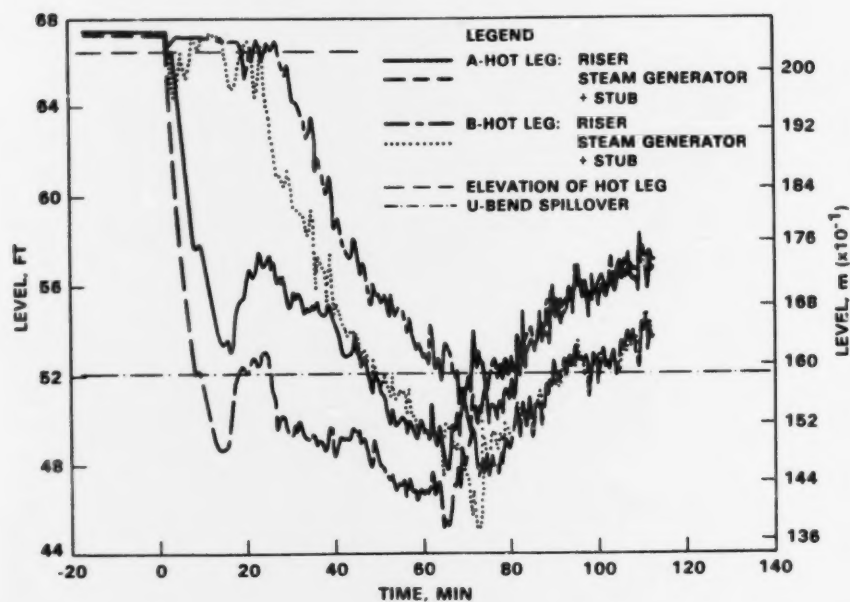


Figure 8. Hot leg riser and stub collapsed liquid levels (Test 3109AA).

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The steam pressure cooldown control temperature difference (core exit minus maximum secondary saturation) reattained 50°F at 45 minutes. Steam Generator B was depressurized to Steam Generator A pressure -- roughly 600 psia -- and both steam generators began to be steamed at 59 minutes. The subsequent reactivation of feed to maintain the 31.6-foot control level at 65 minutes caused a two-loop AFW-BCM. The primary began to depressurize more rapidly from 850 psia (Figure 7). The resulting alterations to the HPI and leak flow rates marked the onset of primary system refill.

At 90 minutes, the primary levels in both steam generators filled above the elevation of the upper steam generator tubesheets while the hot leg riser levels remained well below the U-bend spillover elevation (Figure 8). The primary system began to repressurize, the steam generators became inactive, and the leak flow rate increased to nearly the HPI flow rate. These near-equilibrium conditions persisted for hours. Roughly 3 hours later, the A-loop hot leg riser level attained the U-bend spillover elevation (Figure 9), triggering a pronounced recoupling event.

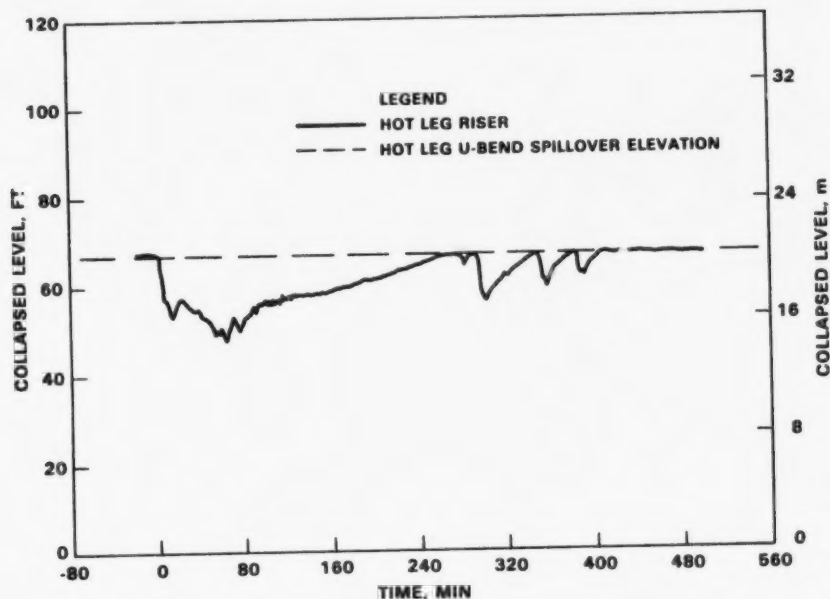


Figure 9. Loop A collapsed levels (Test 3109AA).

Flow spillover in the hot leg initiated heat transfer with Steam Generator A. The upflow of core-region fluid brought the highly subcooled core inlet fluid into the core. The core exit subcooling increased abruptly, triggering both HPI throttling and the automatic and rapid depressurization of the steam generator secondaries. The hot leg levels quickly decreased, however, due predominantly to the HPI throttling. Flow and heat transfer were thus again interrupted, and the primary system conditions realigned toward those

preceding the spillover event. The spillover reinterruption sequence was repeated five times over roughly 2 hours, from approximately 5 to 7 hours after test initiation, until the B-loop remained refilled, circulating, and removing heat. The A-loop hot leg remained voided, however (Figure 9). The test was terminated after 12 hours (based on the maximum test duration criteria).

LEAK/HPI CONFIGURATION, 5 cm², COLD LEG DISCHARGE BREAK

The facility was initialized in steady-state natural circulation for the 5 cm² (0.005 ft²) test (Test 320101). The steady-state conditions were similar to those noted on Table 3 for Test 3109AA. Test 320101 was initiated by opening a 5 cm² (0.005 ft²) leak in the B1 cold leg discharge piping. The operator actions noted for Test 3109AA were also performed in this test when the pressurizer level reached approximately 19 feet. During the transient, HPI, steam generator levels, and steam pressure cooldown controls were operated as in Test 3109AA.

Upon leak actuation (time zero), the primary system depressurized from 1730 psia to 1560 psia at 4.2 minutes (Figure 10). After this time, the depressurization rate increased with the depletion of the saturated pressurizer fluid inventory. At 5 minutes, the primary system depressurization rate slowed at 1480 psia indicating that saturation of the loop fluid occurred. The core exit and hot leg fluid temperatures were briefly equal to the decreasing primary system saturation temperature and the hot leg levels, particularly the A-loop level downstream of the U-bend, were briefly perturbed. However, the excess of HPI flow over leak flow refilled the hot legs and precluded interruption. Beyond 6 minutes and for the duration of the test, the core exit and hot leg fluid temperatures remained subcooled and natural circulation continued.

The flow rates in all four cold legs first increased from roughly 4.2% to 6% of scaled full cold leg flow rates (Figure 11) in response to the actuation of full head auxiliary feedwater at 4.4 minutes. The auxiliary feedwater increased and elevated the natural circulation loop thermal centers. Filling of the steam generator secondaries to the 31.6-foot control level was completed at 12 minutes. The system conditions were responsive to the termination of auxiliary feedwater. The cold leg flow rates dropped from 4.8% to 2.6% (of scaled full cold leg flow rates). The primary system, which had gradually depressurized to 1350 psia during the filling of the steam generator secondaries, began to repressurize slowly (Figure 10b) with the termination of AFW at 12 minutes. The steam generator secondaries had also been depressurized during the secondary side fill (Figure 10b). The steam generator secondaries repressurized from 810 psia at 12 minutes to 875 psia at 14 minutes, with auxiliary feedwater interrupted. The difference between the core exit fluid and the steam generator secondary saturation temperatures reached the 50°F steam pressure cooldown control setpoint at 13 minutes, and the steam generators began to be depressurized by steaming to effect a 100°F/hour cooldown. Both generators began to be steamed at 14 minutes and feedwater became active to maintain level at the 31.6-foot control level. The

system thus became aligned for a 100°F/hour cooldown with the primary in subcooled natural circulation.

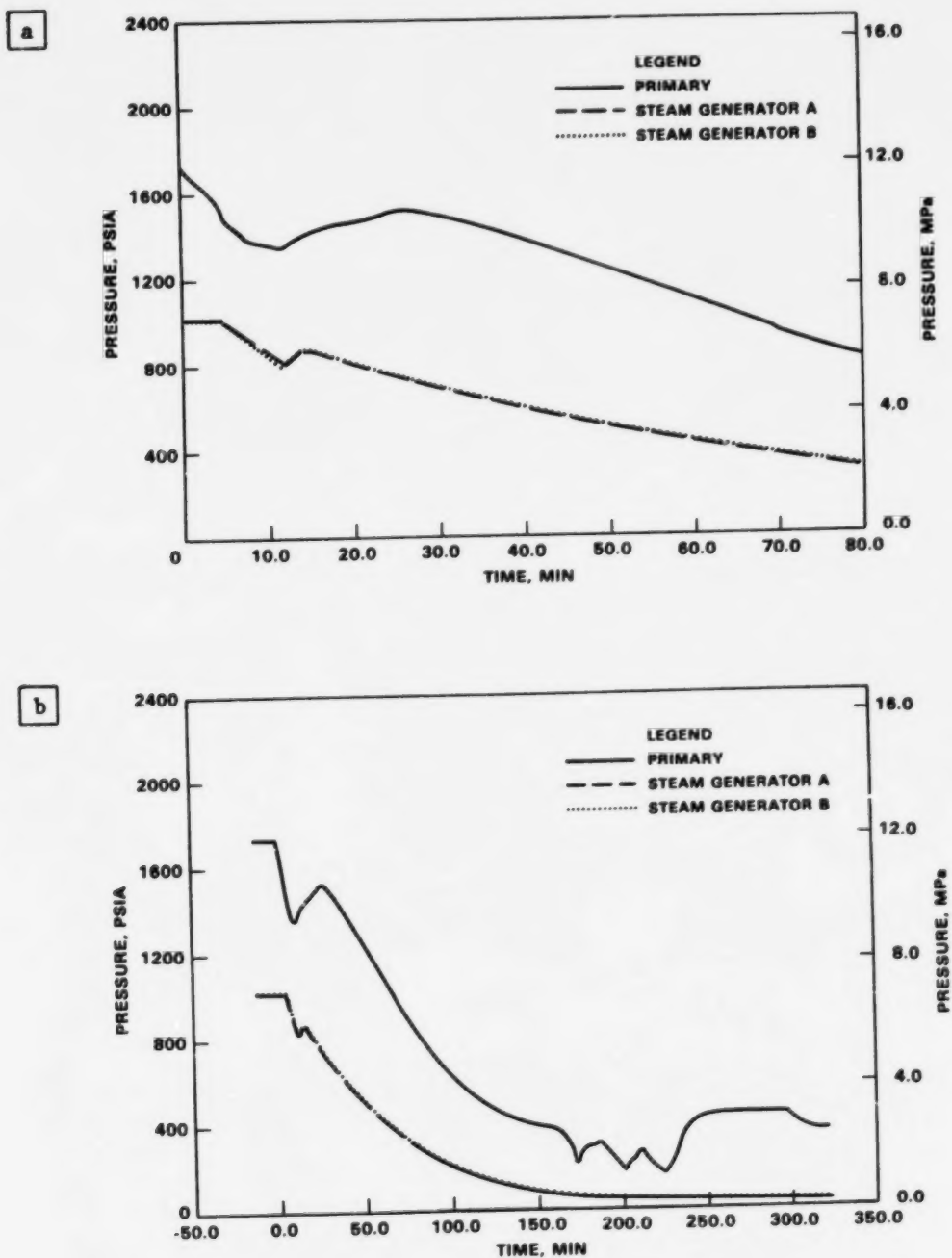


Figure 10. Primary and secondary system pressures (Test 320101).

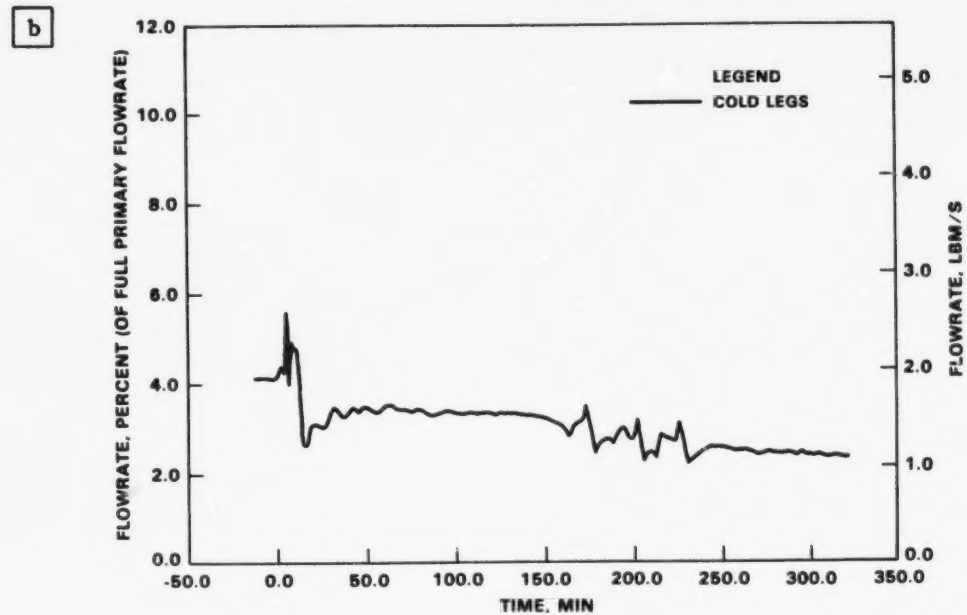
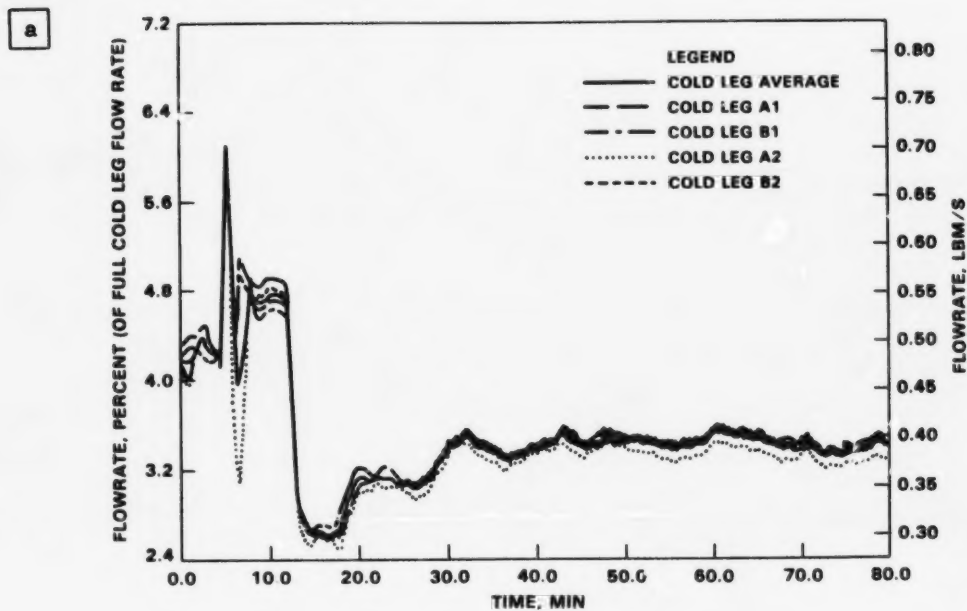


Figure 11. Cold leg flow rates (Test 320101).

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The core-exit subcooling margin (SCM) approached the 70°F control point at 26 minutes, triggering automatic throttling of HPI. HPI was gradually reduced, the SCM was maintained near 75°F, and the primary system pressure began to decrease from 1525 psia at 26 minutes (Figure 10b). At 40 minutes, the primary saturation temperature was reduced below the core outlet temperature at test initiation, and the reactor vessel upper head began to void (Figure 12). The voided region gradually expanded with the continuing primary system depressurization until, at 70 minutes, the reactor vessel upper head void extended to the elevation of the RVVVs and the void growth stopped.

The relatively stagnant upper reactor vessel downcomer fluid saturated at 127 minutes as the primary system saturation temperature continued to decrease (Figure 12b). The downcomer then gradually voided through the elevation of the RVVVs (the RVVVs were closed). Beyond approximately 125 minutes, the HPI could no longer be throttled sufficiently to maintain the 75°F core-exit subcooling. The SCM increased to 100°F at 162 minutes. The operator then stopped HPI as specified and allowed the SCM to diminish to 50°F. The resulting perturbation of system conditions was evident by the indications of primary system pressure, loop flow rate, and downcomer level, but the ongoing cooldown continued unabated. The SCM reached 50°F at 172 minutes, prompting the reactivation of HPI. This HPI off/on sequence was repeated two additional times, then HPI was left active as specified; the SCM then gradually increased to approximately 150°F. The steam generator secondary systems approached their minimum pressures at 200 minutes (Figure 10b). The test was terminated at 300 minutes because of the attainment of stable system conditions.

STEAM GENERATOR TUBE RUPTURE, SINGLE TUBE RUPTURE

The facility was initialized in natural circulation as in the previously described tests. The tube rupture test (Test 340201) was initiated by opening a primary-to-secondary flow path simulating the single-end rupture of one tube at the top of Steam Generator B. The remaining initiation actions were performed immediately after the tube leak was opened. These actions were: activation of HPI with full head/full characteristics; transfer reactor vessel vent valves to automatic; start the core power decay; and initiate refill of the secondary side of both steam generators to 20.7 feet using full head/flow auxiliary feedwater characteristics.

The primary system pressure decreased slightly, from 2150 psia to 2080 psia at 1 minute (Figure 13). The pressurizer level varied only a few feet during test initiation. Within 1 minute after test initiation, the operator began manually throttling the HPI flow rate to control pressurizer level to 21 feet.

Between 5 and 6 minutes after test initiation, the steam generator secondary levels achieved the control values of 20.7 feet and the core-exit subcooling margin (SCM) exceeded 75°F. The former event caused AFW to both steam generators to terminate. The abrupt decrease of steam generator heat transfer caused the loop flow rates to decrease abruptly, from 5% to roughly 2% of scaled full flow (Figure 14).

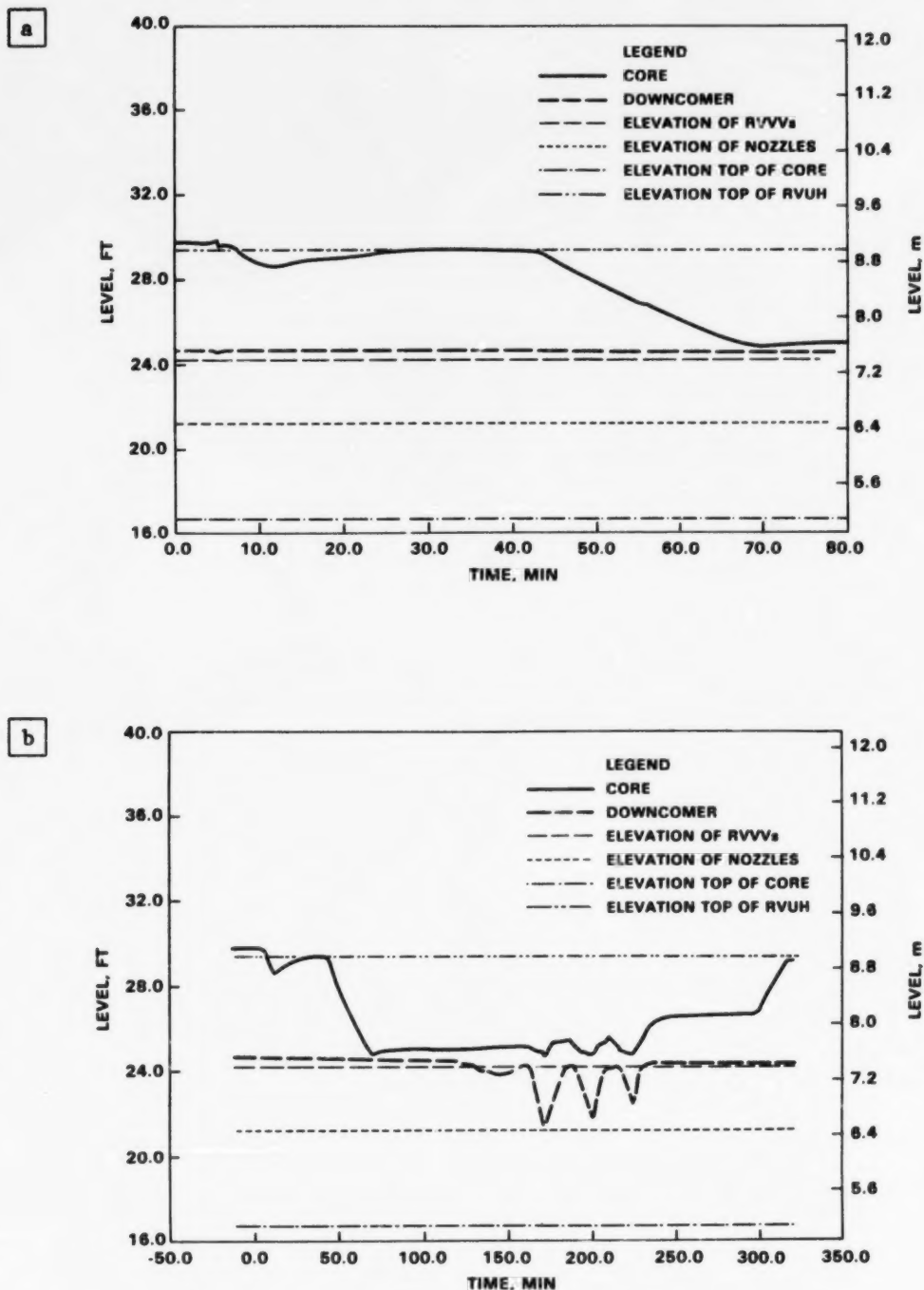


Figure 12. Core region collapsed liquid levels (Test 320101).

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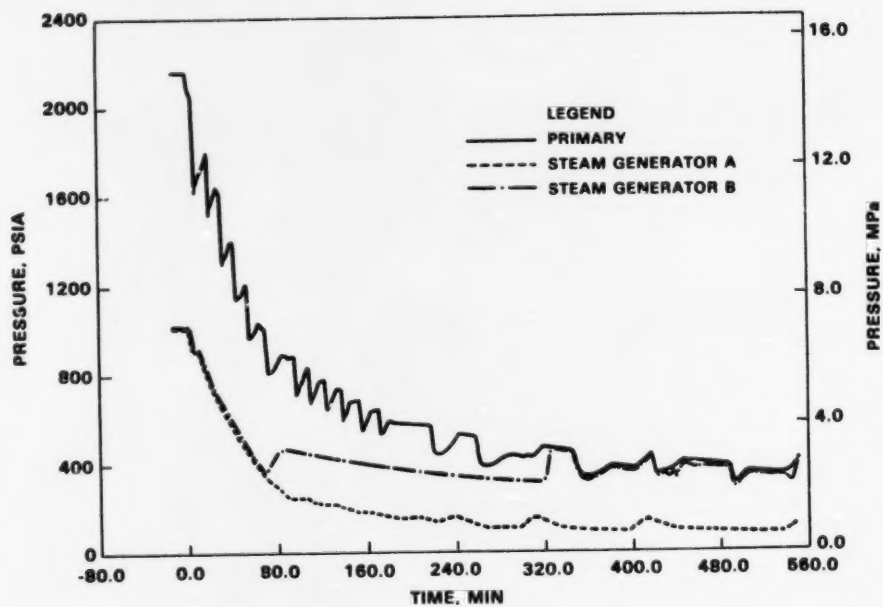
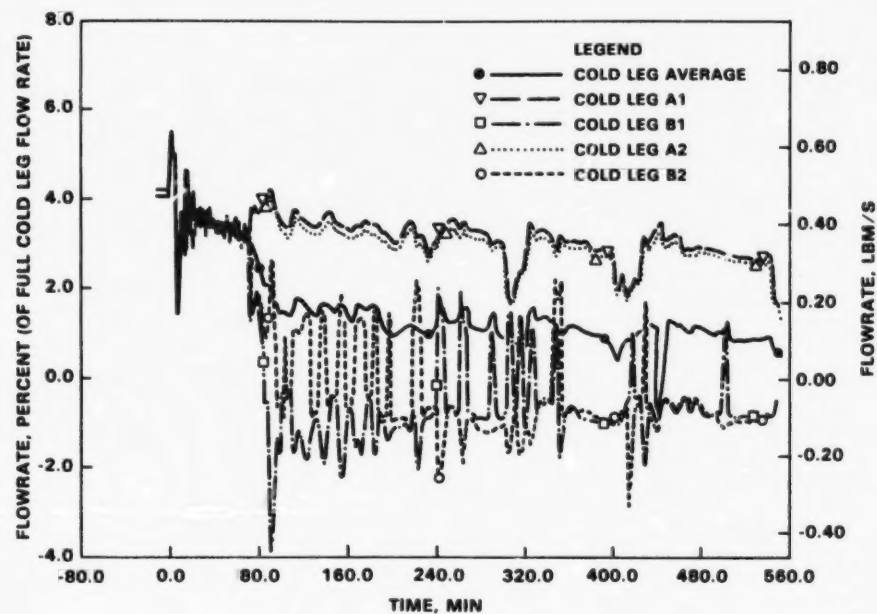


Figure 13. Primary and secondary system pressures (Test 340201).

a



b

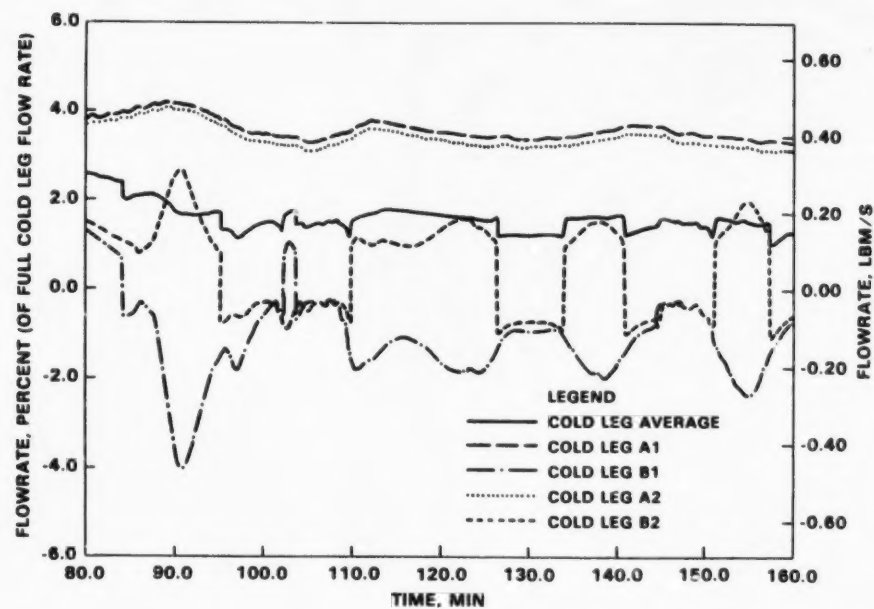


Figure 14. Cold leg flow rates (Test 340201).

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The operator was to maintain the SCM between 50°F and 75°F using PORV actuation. Also, the pressurizer heaters were to be energized at 60°F and deenergized at 70°F SCM. The PORV was opened at 5.3 minutes. The primary system depressurized from 2050 psia at 5.3 minutes to 1600 psia at 7.3 minutes. The operator closed the PORV and energized the pressurizer heaters at 7.3 minutes.

The continuing sequence of PORV manipulations and continuing cooldown were repetitive and uneventful. The temperature difference across the heat transfer components (core and steam generators) gradually rose, and the time-averaged loop flow rates (Figure 14) gradually declined, in response to the decreasing natural circulation driving head. The driving-head decrease was caused by the decreasing core power and by the decreasing thermal expansion coefficient of water (with the decreasing average primary-system fluid temperatures).

The primary system depressurization due to PORV actuation at 30 minutes lowered the saturation temperature to 578°F. The initial core outlet temperature of 593°F set the current reactor vessel upper-elevation fluid temperature; thus, the reactor vessel began to void. The voided volume successively expanded toward the elevation of the vent valves during the PORV actuations.

The steam generator with the tube leak was isolated when the primary was depressurized below 950 psia at 70 minutes (Figure 13). As Steam Generator B AFW and steaming were terminated, the B-loop flow rates dropped abruptly from 3% to nearly 1% of scaled full flow. Steam Generator B secondary began to gradually repressurize from 350 psia at approximately 10 psi/minute. The generator secondary level began to increase at approximately 10 feet/hour from 21 feet (Figure 15).

The B-loop cold leg flow rates stabilized near 1-1/2% (of scaled full cold leg flow rate each) beyond 70 minutes, then began to diminish further beyond 80 minutes (Figure 14). At 87 minutes, relatively cold fluid was displaced backward over the B1 cold leg spillover (at the pump) and the B-loop cold legs began to interact (Figure 14). The flow directions were backward in cold leg B1 and forward in cold leg B2, the net B-loop flow was virtually zero.

The periodic PORV actuations maintained the core-exit SCM within the desired band, gradually lowered the time-averaged primary pressure, and hence decreased the pressure difference across the ruptured tube. The PORV actuation from 153 to 155 minutes lowered this pressure difference across the break from 280 psi to 125 psi. The resulting reduction of break flow rate momentarily reduced the rate of refill of the Steam Generator B secondary (Figure 15).

The PORV actuation from 218 to 222 minutes saturated the generally stagnant B-loop hot leg fluid. The pressurizer level, which was being controlled near 21 feet by manual HPI throttling, quickly rose to 24 feet as the B-loop U-bend region voided. The B-loop voiding persisted from 220 to 240 minutes and achieved B-loop hot leg riser and stub levels as low as 59 feet -- some 7 feet below the U-bend spillover elevation (Figure 16).

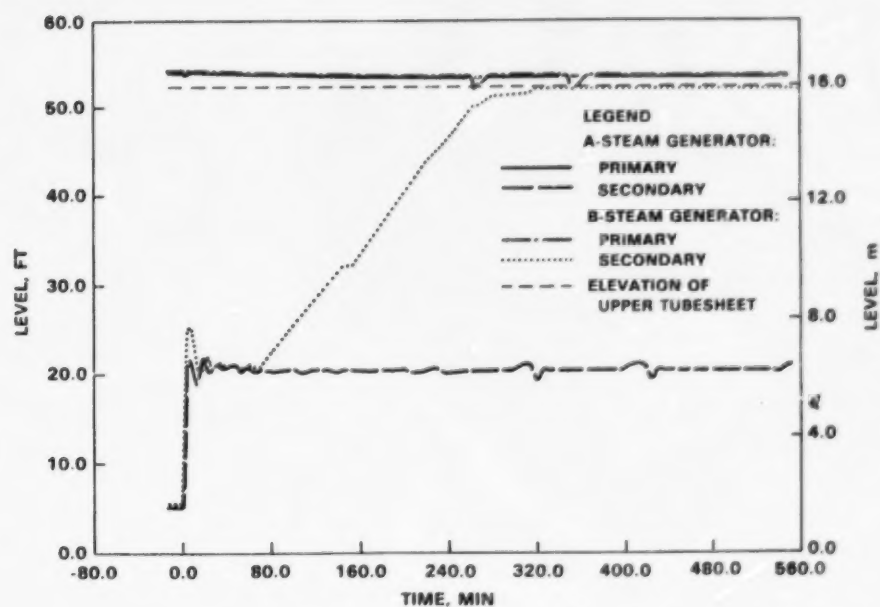


Figure 15. Steam generator collapsed liquid levels (Test 340201).

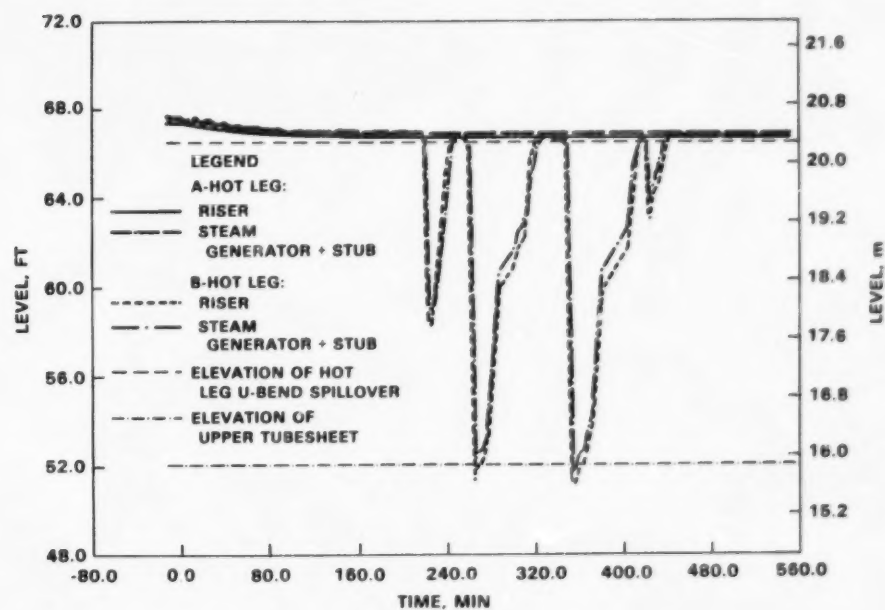


Figure 16. Hot leg riser and stub collapsed liquid levels (Test 340201).

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The rate of refill of the isolated Steam Generator B secondary had been gradually diminished with primary depressurizations. As the primary-to-secondary pressure difference was reduced with the PORV kept closed from 264 to 347 minutes, the Steam Generator B secondary filled completely at 318 minutes. The secondary then quickly pressurized to the primary system pressure -- 450 psia at 322 minutes (Figure 13). The B-loop hot leg riser and stub were refilled to the U-bend spillover elevation at 320 minutes after the refill of the isolated steam generator secondary.

The test was terminated at 540 minutes based on the maximum duration-termination criteria.

REFERENCES

1. Coordination of Support Projects for the B&W Integral System Test Program, edited by J. P. Sursock and M. W. Young, NUREG-1163.
2. H. R. Carter, "Integral System Test Program," Babcock & Wilcox Operating Experience Seminar, Lynchburg, Virginia, 1983.
3. H. R. Carter and J. R. Gloudemans, "An Experimental Study of the Post Small-Break Loss-of-Coolant Accident Phenomena in a Scaled Babcock & Wilcox System," NUREG/CP-0058, Proceedings of the U.S. Nuclear Regulatory Commission, Twelfth Water Reactor Safety Research Information Meeting, October 1984, Vol. 1, pp. 113-135.
4. E. H. Davidson and R. L. Black, "Research Relating to Reactor Vessel Head Vent Exemption: Core Cooling by Natural Circulation and Feed/Bleed in the Presence of Noncondensibles," NUREG/CP-0058, Proceedings of the U.S. Nuclear Regulatory Commission, Twelfth Water Reactor Safety Research Information Meeting, October 1984, Vol. 1, pp. 136-152.
5. R. K. Fujita and T. D. Knight, "TRAC-PF1/MOD1 Support Calculations for the MIST/OTIS Program," NUREG/CP-0058, Proceedings of the U.S. Nuclear Regulatory Commission, Twelfth Water Reactor Safety Research Information Meeting, October 1984, Vol. 1, pp. 153-173.
6. D. P. Birmingham, R. L. Black, and G. C. Rush, "An Experimental Study of the Application of Abnormal Transient Operating Guidelines (ATOG) to the Babcock & Wilcox Nuclear Steam Supply System During a Small-Break Loss-of-Coolant Accident," ASME/AICHE/ANS National Heat Transfer Conference, Denver, Colorado, August 6-9, 1985.
7. M. T. Childerson and R. K. Fujita, "TRAC-PF1 Code Verification With Data From the OTIS Test Facility," Presented at the 106th ASME Winter Annual Meeting, November 1985.

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REFERENCES (Continued)

8. Y. A. Hassan and G. C. Rush, "A New Technique for Estimation of Void Fraction From Conductivity Probe Signals in a Small-Break Loss-of-Coolant Accident Test Facility," Presented at the 106th ASME Winter Annual Meeting, November 1985.
9. J. R. Gloudemans, "OTIS Test Results," Presented at the Thirteenth Water Reactor Safety Research Information Meeting, October 1985.
10. H. R. Carter, "MIST Facility Status," Presented at the Thirteenth Water Reactor Safety Research Information Meeting, October 1985.
11. J. R. Gloudemans, "Simulation of Reactor Vessel Vent Valves," Presented at the 106th ASME Winter Annual Meeting, November 1985.

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TRAC-PF1/MOD1 PRETEST PREDICTIONS OF MIST EXPERIMENTS*

by

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ABSTRACT

Los Alamos National Laboratory is a participant in the Integral System Test (IST) program initiated in June 1983 to provide integral system test data on specific issues and phenomena relevant to post small-break loss-of-coolant accidents (SBLOCAs) in Babcock & Wilcox plant designs. The Multi-Loop Integral System Test (MIST) facility is the largest single component in the IST program. During Fiscal Year 1986, Los Alamos performed five MIST pretest analyses. The five experiments were chosen on the basis of their potential either to approach the facility limits or to challenge the predictive capability of the TRAC-PF1/MOD1 code. Three SBLOCA tests were examined which included nominal test conditions, throttled auxiliary feedwater and asymmetric steam-generator cooldown, and reduced high-pressure-injection (HPI) capacity, respectively. Also analyzed were two "feed-and-bleed" cooling tests with reduced HPI and delayed HPI initiation. Results of the tests showed that the MIST facility limits would not be approached in the five tests considered. Early comparisons with preliminary test data indicate that the TRAC-PF1/MOD1 code is correctly calculating the dominant phenomena occurring in the MIST facility during the tests. Posttest analyses are planned to provide a quantitative assessment of the code's ability to predict MIST transients.

INTRODUCTION

Los Alamos is currently providing analytical support to the Integral System Test (IST) program; the largest part of our analytical efforts involves the use of the TRAC-PF1/MOD1 code. TRAC is, or will be, used as a complement to the Multi-Loop Integral System Test (MIST) experimental program in three applications. The first of these is related to test specification or design. This assumes that one has sufficient confidence that TRAC will correctly predict the dominant test phenomena and, therefore, that the predicted results can be used to ensure that facility limits will not be exceeded and that the resultant test will satisfy the test objectives. For example, TRAC can be used to determine whether the design limits of the facility or facility instrumentation ranges could be exceeded during a test. The

* Work performed under the auspices of the US Nuclear Regulatory Commission.

second application is related to test evaluation. It is impossible to include all the desired instrumentation in a facility because of constraints such as cost, complexity, and space. Again, if one has sufficient confidence that TRAC will correctly predict the dominant test phenomena, calculations can be used to fill in gaps about quantities that are not measured in the facility. The third application is related to TRAC assessment. The ability of a thermal-hydraulic code to accurately calculate experimental behavior in scaled facilities is an important link in demonstrating that the code can be used to predict how an operating pressurized water reactor (PWR) would perform under accident conditions.

During Fiscal Year 1986, Los Alamos performed five MIST pretest analyses. The five experiments were chosen on the basis of their potential either to approach the facility limits or to challenge the predictive capability of the TRAC code. The five tests are identified and briefly discussed in the following paragraphs.

Test 310000. MIST test 310000 was the proposed nominal test for the MIST program. During the early test program, several repeats of this test were run. The test selected for the nominal, Test 3109AA, differs from the pretest specification for 310000 in the initial pressurizer liquid level and in efforts to warm the surge line and maintain the pressurizer liquid at saturation until test initiation. Thus, the TRAC pretest calculation was made for conditions slightly different from those in the selected nominal Test 3109AA. The nominal conditions include a scaled 10-cm² cold-leg (CL) discharge leak, full high-pressure injection (HPI) and auxiliary feedwater (AFW), unavailable reactor-coolant pumps (RCPs), no noncondensable gas injection, automatic reactor vessel vent-valve (RVVV) actuation on differential pressure, automatic guard heater control, constant steam-generator (SG) secondary level control after SG refill, and symmetric SG cooldown.

Test 310503. This test focuses on the effect of throttled AFW and asymmetric SG cooldown. The longer-duration SG refill obtained by reducing the SG-refill rate is intended to obtain AFW boiler-condenser mode (BCM) cooling. Following SG refill, the SGs are to be depressurized at unequal rates, to generate asymmetric loop conditions. The purpose is to accentuate interloop asymmetries to investigate their long-term impact.

Test 320604. The tests of group 32 are controlled in the same manner as the Nominal Test (310000) but with altered leak and HPI characteristics. This test uses the Evaluation Model (EM) HPI capacity, which is roughly one-half that of full HPI. Relative to the nominal case, extensive system voiding and perhaps an early occurrence of the BCM are expected.

Test 330201. The tests of group 33 examine system interactions that occur while using feed-and-bleed (HPI - PORV) cooling. These tests simulate a total loss of secondary-side feedwater. Test 330201 uses the EM rather than the full HPI head-flow characteristics. EM HPI flow is initiated upon lifting of the power-operated relief valve (PORV).

Test 330302. This test imposes delayed HPI activation. The PORV discharge will be the only available energy-removal mechanism for the first 20 minutes following PORV lift. The full HPI is then provided.

Each of the five experiments described above has now been run in the MIST facility. Formal test reports and the data tapes are not currently available. However, limited results from several of the tests are available and can be used for qualitative assessment of TRAC's ability to predict the dominant phenomena in the tests. We will include a brief discussion, where test results are available.

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CODE VERSION

We used an updated version 12.7 of the TRAC-PF1/MOD1 code (Ref. 1). The TRAC-PF1/MOD1 code was developed at Los Alamos National Laboratory to provide advanced best-estimate predictions of postulated accidents in light-water reactors. The code features a two-phase, two-fluid nonequilibrium hydrodynamics model with a noncondensable gas field; flow-regime-dependent constitutive equation treatment; either one- or three-dimensional treatment of the reactor vessel; complete control-systems modeling capability; a turbine component model; and a generalized steam-generator component model.

PLANT MODEL

Figure 1 is a MIST facility arrangement drawing. Figures 2 and 3 provide an overview of the TRAC MIST facility model. The TRAC model of the MIST facility has evolved over a period of time. The model was initially based on preliminary information provided in the MIST Facility Specification. It has progressed to its present form as available as-built facility information was received from Babcock & Wilcox (B&W). The model consists of 77 components that have been subdivided into 276 fluid cells. Only one-dimensional components are used in this model. The model is considered to be rather finely noded and is expected to predict the dominant phenomena during MIST experiments.

CALCULATION RESULTS

During analysis of our pretest calculations, we found that many of the predicted phenomena occurred in each of the calculations. Therefore, we have chosen to provide a detailed description of the nominal MIST Test 310000 and will provide less detailed descriptions for the remainder of the pretest predictions. The discussion in these cases will focus on the important phenomena that differ from those in the nominal case. We note that for each test calculation, at time zero, the primary was liquid full and coolant was being driven by natural circulation.

Test 310000. The transient calculation was started at time zero by the initiation of a scaled 10 cm^2 break just downstream of the HPI nozzle in the B1 cold leg (Fig. 1). As the pressurizer level fell below 0.3048 m (1 ft.) (at 45 s), several actions were taken. The core power ramp was started, HPI began, RVVV automatic control was initiated, and the RVVVs first opened.

Primary-system saturation first occurred at 80 s in the intact-loop hot leg (Fig. 4) as a result of the effects of the pressurizer liquid entering that leg. Natural circulation in the intact loop ended at 160 s (Fig. 5) when the hot-leg U-bend fully voided. This effectively decoupled the intact-loop SG as a heat sink. The broken-loop (B) hot leg first saturated at 110 s, for about 40 s duration. During this time, the void fraction remained small and there was little effect on the loop natural circulation. At about 420 s, the broken loop again saturated. This time flow through the loop declined rapidly until about 530 s, when the loop temporarily stalled. The declining primary-to-secondary heat transfer, caused by the decline in broken-loop flow, led to a temperature increase at the core exit beginning at about 440 s. The core exit liquid saturated at 500 s with the increasing temperature. This caused the system to begin repressurization (Fig. 6). As the broken loop stalled at 530 s, voiding in the reactor vessel upper head forced more liquid into the hot legs, immediately restarting flow through the broken-loop. This continued until 860 s when the U-bend voided completely and

the loop again stalled. The increased primary-to-secondary heat transfer from the renewed flow through the hot leg, along with leak-HPI cooling and the declining core power, caused the system to begin depressurizing again at about 725 s. An intraloop natural circulation started in the broken-loop cold legs just after circulation through the loop ended (Fig. 7). This flow goes from the downcomer, backwards through the broken cold leg, and forward through the other broken-loop cold leg.

Steam generator secondary depressurization is automatically controlled to a set point based on abnormal transient operator guidelines (ATOG) determined from core-exit temperature and the saturation temperatures corresponding to SG secondary pressures. Depending on these three temperatures, the ATOG-based set point pressure may be: (1) held constant, (2) reduced by 50 psi/min, or (3) reduced such that the corresponding saturation temperature is reduced by 100°F/h. Following reactor trip at 45 s, the SG control system received a signal to fill the secondaries to the 9.63 m (95%) level. The secondary-side collapsed liquid levels of the intact-loop and broken-loop SGs reached this level at about 500 and 540 s, respectively. During the refilling of the secondaries by the AFW, the secondary pressures were reduced (Fig. 6). In the intact loop, the secondary pressure remained below the ATOG-based set point pressure, resulting in termination of steam flow by the control system. Therefore, with the exception of a small flow as the control system settled in on the level set point, there was no supply of AFW to the intact-loop secondary after 500 s until the ATOG-based set point decreased to the intact-loop SG pressure at 3500 s. The broken-loop secondary pressure remained below the ATOG-based set point until 760 s. The controller is designed to allow steaming of the broken-loop SG secondary to maintain the set point pressure and provide AFW flow to maintain the liquid level; BCM heat transfer can occur when the primary liquid in the SG drops below the upper tube sheet and there is AFW flow. An event of this type occurred at about 2600 s and increased the rate of primary depressurization (Fig. 6). At 3500 s, when the ATOG-based set point reached the intact-loop SG secondary pressure, allowing steaming and AFW flow, the liquid level in the primary side was well below the tube sheet. This created a strong BCM event that increased the primary depressurization rate significantly (Fig. 6).

Refill started at about 3600 s when two-phase flow out the break started and HPI flow exceeded break flow (Fig. 8). It is expected that the break and HPI flows will have nearly the same magnitude for some time, with break flow gradually decreasing and HPI gradually increasing as the system depressurizes. The calculation was run to 3696 s, which is near the start of this phase. System liquid inventory should then gradually increase.

During nominal Test 3109AA, the following phenomena were observed: (1) interruption of natural circulation in the intact loop with continued intermittent natural circulation in the broken loop before complete interruption of circulation; (2) strong loop asymmetries in the hot-leg liquid levels and temperatures and in the cold-leg flow rates and temperatures; (3) weak SG performance without liquid flow through the loops resulting from BCM heat transfer and condensation in the SG primary side; and (4) intraloop natural circulation with flow from the downcomer, backward through one cold leg and forward through the adjacent cold leg. Each of these phenomena was predicted by TRAC. We note that there were significant quantitative differences between the predicted transient and the selected nominal Test 3109AA. However, the MIST facility has proven to be a very sensitive facility with strong coupling within the primary system and from the primary to the secondary system. A true quantification of

TRAC's ability to predict the MIST test must await posttest assessment efforts using the initial and boundary conditions measured during the test.

Test 310503. As in MIST Test 3109AA, the nominal test, the transient calculation was started at time zero by the initiation of a scaled 10-cm² break in the B1 cold leg just downstream from the HPI nozzle. The same initial control actions were taken for Test 310503 as for Test 310000, except that the SG refill rate was reduced. This was intended to alter the primary-to-secondary heat-transfer rates during SG refill by increasing the possibility for BCM heat transfer, and to change the system interactions. Later in the transient, asymmetric SG cooldown, with the SG depressurization rates controlled to decrease the intact-loop SG secondary saturation temperature by 100°F/h and the broken-loop secondary by 50°F/h, was used. The test was expected to provide data on the effect of accentuated asymmetries between the loops.

Many of the phenomena seen before the SGs refill to the set points that signal the beginning of controlled SG-secondary depressurization were similar to the phenomena predicted for the nominal transient. The primary-to-secondary heat transfer predicted was less for this case, which had a reduced SG-secondary refill rate, than for the proposed nominal case, Test 310000. Consequently, the depressurization rate was initially slower (Fig. 9) and the leak flow was higher. The interruptions of the loop flows are very similar to the interruptions seen in the nominal case, although the loop flows (Fig. 10) were smaller and these events occurred at different times. The intact loop saturated, the U-bend voided, and the loop stalled in a slightly longer time than calculated for specified Test 310000. This happened because the smaller primary-to-secondary heat transfer resulted in a slower depressurization rate and initial saturation was not reached as quickly. The broken loop continued in natural circulation, saturated, and the flow decreased. This decreased the primary-to-secondary heat transfer and resulted in a heating up of the liquid exiting the core. It saturated, and as the broken loop was about to stall, voiding in the reactor vessel forced more liquid over the broken-loop U-bend, giving renewed flow. Eventually the U-bend voided fully and flow through the broken loop ceased. The final loss of natural circulation through the broken loop occurred several minutes earlier for this case than for the proposed nominal case because of the higher leak flow calculated for this test.

The lower SG-secondary refill rate gave continued AFW flow after both loops stalled. In the nominal case the refill had ended before the broken-loop stalled. As a consequence, this case had a greater depressurization rate after the loops stalled than did the nominal case. The primary pressure is lower for this case than for the nominal case after about 2000 s. After SG-secondary refill was completed at about 2800 s, the rate of depressurization slowed considerably.

The system slowly depressurized from the time the AFW ended at about 2800 s until about 4600 s, when the set point for the intact-loop SG secondary depressurization control declined to the intact-loop SG secondary pressure. At that time, steaming and AFW to the intact-loop SG secondary resulted in a strong feed-cycle BCM event, and a corresponding sharp increase in the primary depressurization rate. This increased the boiloff and level depression in the broken-loop SG primary so that it dropped below the level of the secondary side pool, giving pool BCM heat transfer starting at about 4700 s. This caused the secondary

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pressure in the broken-loop SG to increase until it reached the set point pressure for controlled depressurization at about 5200 s.

Refill started at about 4700 s (Fig. 11), when the more rapid depressurization caused two-phase flow out the break. HPI flow exceeded break flow for the remainder of the calculation, although the two were approximately balanced. This condition is expected to continue, with HPI flow gradually increasing and break flow gradually decreasing.

Test 320604. Test 320604 was identical to the proposed nominal test except that the HPI capacity was roughly one-half of full HPI flow. With the lower HPI flow rate, the primary pressure (Fig. 12) and vessel inventory (Fig. 13) decreased more rapidly than in the nominal case. The intact-loop hot leg saturated at about 80 s and remained saturated for the remainder of the transient. At about 130 s, the intact-loop U-bend was fully voided; natural circulation flow through the SG primary ended about 5 s later (Fig. 14). This effectively decoupled the intact A-loop SG as a heat sink. The broken loop saturated briefly (for about 20 s) starting at 100 s; until 580 s it saturated intermittently. After that time it remained saturated with complete voiding in the U-bend occurring at 610 s and complete loss of natural circulation flow occurring about 3 s later (Fig. 14). This was slightly more than half the time for the nominal case to lose all natural circulation through the hot legs.

Following reactor trip at 45 s, the steam generator control system received a signal to fill the secondaries to the 9.63 m (95%) level. The secondary side collapsed liquid levels of the intact-loop and broken-loop SGs reached this level at about 500 and 540 s, respectively. During the refilling of the secondaries by the AFW, the secondary pressures were reduced (Fig. 12). In the intact loop, the secondary pressure remained below the ATOG set point pressure resulting in termination of steam flow by the control system. Therefore, with the exception of a small flow as the control system settled in on the level setpoint, there was no supply of AFW to the intact loop secondary after 500 s until the ATOG setpoint decreased to the intact loop steam generator pressure at 7000 s (Fig. 12). At this time steam flow was reestablished in the intact loop steam generator and AFW was then activated in the intact loop to control the steam generator level. The AFW startup at 7000 s caused a strong feed cycle BCM since the liquid level in the primary side of the intact-loop SG tubes was well below the AFW elevation this time.

In the broken loop, the ATOG-based set point pressure was recovered shortly after the SG was refilled at 540 s, since there was natural circulation in the broken loop at this time (Fig. 14). Afterward, the broken-loop SG pressure was controlled to the decreasing ATOG set point by a positive steam flow and the secondary level was maintained by a positive AFW flow. At about 1300 s, the level inside the tubes in the broken-loop SG decreased below the elevation of the AFW. From this time until 7000 s, a weak feed-cycle BCM was calculated to occur in the broken loop, which rejected some of the decay heat from the primary system. During the 540–7000 s interval, the control system for the ATOG-based pressure control alternately operated in the constant pressure control mode and in the 100°F/hr cool-down mode, depending on the core-exit temperature and the SG secondary pressures. The ATOG-based pressure was generally calculated as the saturation pressure corresponding to a saturation temperature 50°F below the core-exit temperature.

From break initiation to about 2700 s, the break discharge flow exceeded the HPI flow and the primary system inventory was being depleted. Although there were earlier periods of

two-phase flow at the break, beginning at 2700 s, periods of single-phase vapor flow out the break were predicted. This reduced the break discharge flow to approximately the HPI flow and the primary system inventory was then stabilized until the intact-loop feed-cycle BCM at 7000 s. At this time, the primary pressure was reduced by heat transfer to the intact-loop secondary and the leak flow was further reduced to a value below the HPI flow. After the start of refill at 7000 s, the primary system inventory gradually increased. The reactor vessel collapsed liquid level (Fig. 13) was calculated to remain at or above the hot-leg nozzle elevation for the duration of Test 320604.

The same phenomena and overall system behavior were calculated for the reduced HPI Test 320604 as for the specified nominal Test 310000. The main effect of the reduced HPI flow was to change the timing of major events. The interruption of natural circulation was calculated to occur earlier in the transient with the reduced HPI flow since the hot legs drained faster. After the interruption of natural circulation, the primary system depressurized more slowly with reduced HPI flow because the feed-and-bleed cooling of the HPI-leak flow was reduced. The minimum primary-system inventory was not strongly affected by the reduced HPI flow in Test 320604 because the leak flow was also reduced as a result of higher void fractions calculated in the cold leg upstream of the leak.

Test 330201. This transient calculation was initiated by the termination of all AFW to the SG secondaries. There were two immediate consequences of AFW termination. First, the heat removal capacity of the steam generators was reduced. Because the primary is liquid solid (full of liquid,) the excess core decay energy was deposited in the primary coolant and the temperature began to rise. The resultant coolant expansion in the solid system caused the primary pressure to rise (Fig. 15). Second, with the cessation of AFW, the SG thermal centers moved to a lower level in the SGs. With the reduction in driving head, the primary loop natural-circulation flows decreased markedly. The natural-circulation flows in the primary loops subsequently recovered as the primary heated up; the first recovery occurred in the intact loop followed by recovery of the broken loop (Fig. 16). However, the loop flows went through several long-period cycles and remained out of phase until natural circulation was terminated by voiding at the top of the hot-leg U-bends.

By 570 s, the primary pressure had increased to the PORV set point value of 16.2 MPa (2350 psi). The PORV opened at its set point and was locked open for the remainder of the transient; this constituted the bleed action in the feed-and-bleed transient. At the same time, the cold-leg HPI flow was initiated; this constituted the feed action in the feed-and-bleed transient. The core power decay was then initiated after a delay of 10 s. After the PORV was opened, the primary pressure rapidly decreased until the fluid in the upper plenum, upper head, and hot legs reached saturation. Shortly after, the top of the intact-loop U-tube voided and intact-loop natural circulation was lost. This also represented the effective loss of the intact-loop SG as a heat sink. As a consequence, the primary system began to repressurize at about 750 s and the boiloff rate in the intact-loop SG was reduced. The boiloff of the liquid in the intact-loop SG secondary was completed by about 1100 s.

By 890 s, the pressurizer was full of liquid and fluid passing through the PORV changed from a two-phase mixture to liquid. Consequently, less energy was relieved from the primary. Core decay energy was again deposited in the primary coolant and caused the core outlet

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liquid temperature to increase. Because only the broken loop remained liquid full, the natural-circulation flow through the broken loop began to increase rapidly. Steam production in the broken-loop SG also increased; sufficient energy was removed that the primary repressurization was briefly terminated from 920 s to 950 s when the broken-loop SG was boiled dry. After 950 s the primary system continued to repressurize until the broken-loop U-bend voided at 1225 s, terminating natural circulation in the broken loop. Shortly afterward, two-phase flow through the PORV was re-established and the primary system repressurization was terminated. The energy relief through the PORV and the core decay heat approached an approximate balance and a slow decline of pressure at the rate of 700 Pa/s (0.102 psi/s) was then established for the remainder of the calculated transient. By 3070 s the primary pressure had reduced to 11.5 MPa (1668 psi).

Test 330302. The phenomena in this calculation were quite similar to those in Test 330201. The transient from initiation to opening the PORV at 568.5 s was identical to that for Test 330201. For this transient, the cold-leg HPI flow initiation was delayed 1200 s until 1787.5.

From the time the PORV opened until HPI actuation, the MIST facility experienced primary-system inventory depletion. Inventory was lost from both the vessel and the primary loops. However, for the period between 1375 to 1640 s, the vessel inventory remained nearly constant; liquid drainage from the cold legs and the reactor-coolant pumps into the downcomer replenished vessel inventory. At 1640 s, the inventory of the cold legs and reactor-coolant pumps is depleted and the vessel inventory begins to decline rapidly as shown in Fig. 17. The collapsed liquid level decreased to the hot-leg nozzles; vapor flow into the hot legs then retarded further drainage of liquid into the vessel upper plenum. Vessel inventory depletion continued until HPI initiation. The HPI flow was sufficient to immediately begin refilling the vessel. Once the vessel mixture level reached the hot-leg nozzles, vapor flow into the hot legs was terminated. Vessel-downcomer natural circulation through the RVVVs was then re-established, and energy relief through the PORV and the core decay heat approached an approximate balance. The primary then settled into a nearly constant rate of depressurization and cooldown until the end of the calculated transient at 2480 s. The calculation was terminated when it was evident that the refilling process would continue without interruption.

CONCLUSIONS AND RECOMMENDATIONS

Results of the five pretest calculations indicate that the MIST facility can be expected to exhibit a characteristic post-SB LOCA behavior under a wide variety of imposed boundary conditions. The initial loop behavior was common to all five of the calculations. After leak initiation or PORV opening, this behavior included the interruption of natural circulation in the intact loop, followed by a sharp reduction in broken-loop natural circulation flow and the start of primary system repressurization. It also included a subsequent period of spillover circulation in the broken loop sufficient to stop the repressurization. After this sequence of events, natural circulation was completely terminated in both loops and the calculated system response depended on the control procedures specified for the test. Strong system interactions and coupling were observed in each of the pretest calculations. For example, during liquid-continuous primary natural circulation, the primary loops experienced several long-period flow oscillations. The presence of the pressurizer in the intact loop was the trigger that initiated the differing thermal-hydraulic behaviors of the intact and broken loops.

The pretest calculations were performed through the blowdown phase of the transient and each calculation was terminated near the beginning of refill when the HPI flow exceeded the leak flow. The MIST scaling and operation atypicalities generally tend to extend the times required for various events to occur and to exaggerate the asymmetries of the facility; however, the overall trends of the calculations were not affected by the scaling atypicalities.

Results of the pretest calculations indicated that the MIST facility safety limits would not be approached in the five tests considered. First, core uncover will not occur during any of the tests examined. The lowest vessel liquid levels were predicted for Test 330302, feed-and-bleed cooling with HPI delayed 20 minutes after PORV actuation. Second PORV capacity is sufficient to maintain the primary-system pressure below the relief valve set points during the feed-and-bleed tests.

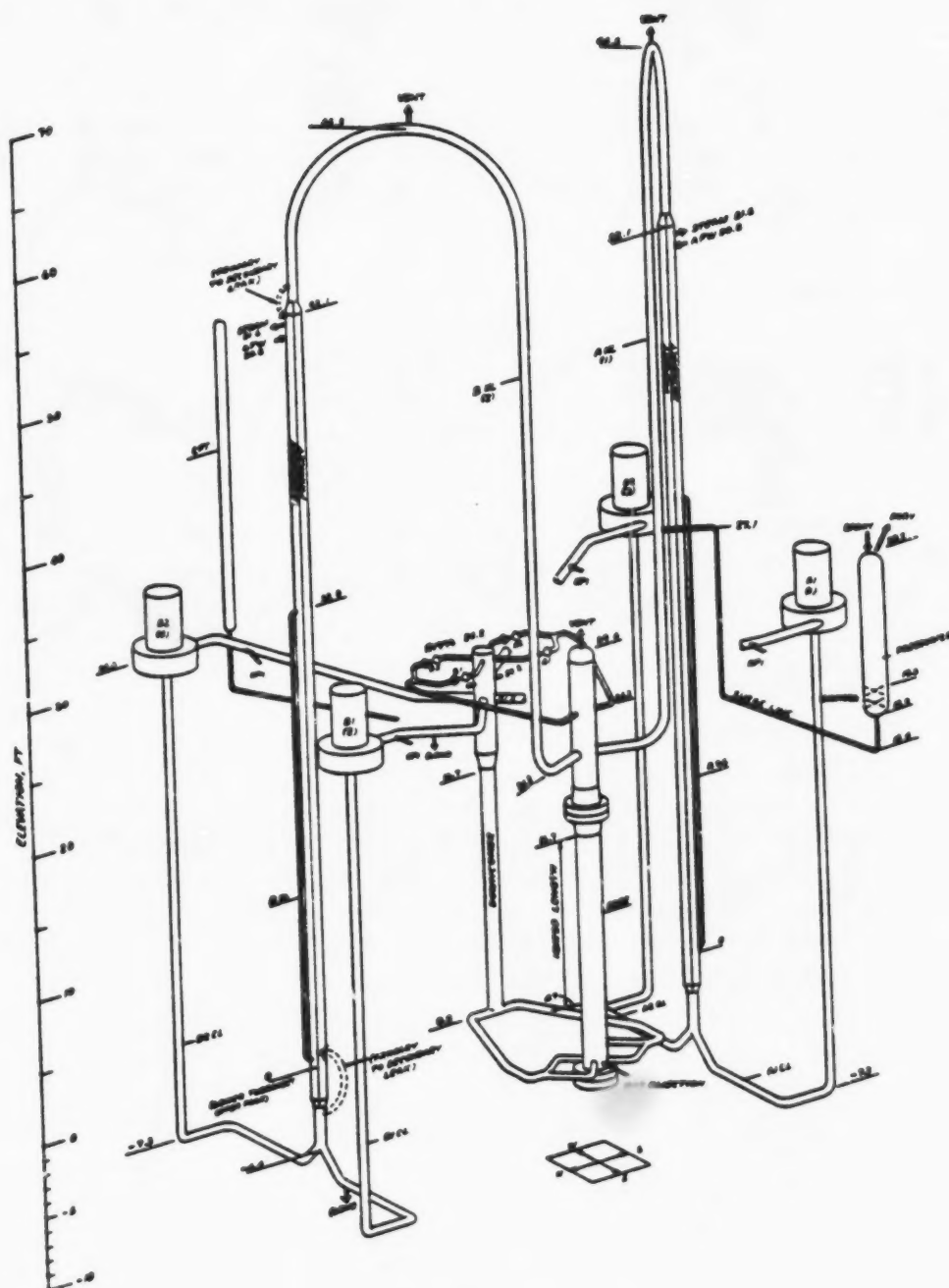
The only observed deficiencies in the code involved the AFW wetting model and the ability to represent cross flows in the secondary. The AFW wetting problem involves the combination of the specified degree of tube wetting on the secondary and the heat-transfer correlations that are invoked; it results in the calculated thermal center of the steam generator being too low and causes the calculated primary natural circulation flow at the beginning of the transients to be low. We do not anticipate this to be a problem in plant calculations except possibly during the early stages of some transients when the secondary filling by AFW occurs after the primary coolant pumps have coasted down and flow is driven by natural circulation. The other deficiency involves the ability of the parallel channel secondary used in these analyses to represent the necessary cross flows; this modeling capability should be improved in TRAC-PF1/MOD2, which will permit one to use the VESSEL component to model steam-generator secondaries.

The results of the pretest calculations will be used as a basis for subsequent assessment of the TRAC-PF1/MOD1 code, which will ultimately be used to extrapolate data from the MIST facility to full-scale plant behavior.

REFERENCES

1. Safety Code Development Group, "TRAC-PF1/MOD1: An Advanced Best-Estimate Computer Program for Pressurized Water Reactor Thermal-Hydraulic Analysis," Los Alamos National Laboratory report LA-10157-MS (NUREG/CR-3858) (July 1986).

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MIST Arrangement
(HORIZONTAL DIMENSIONS - NOT TO SCALE)

Fig. 1.
MIST facility.

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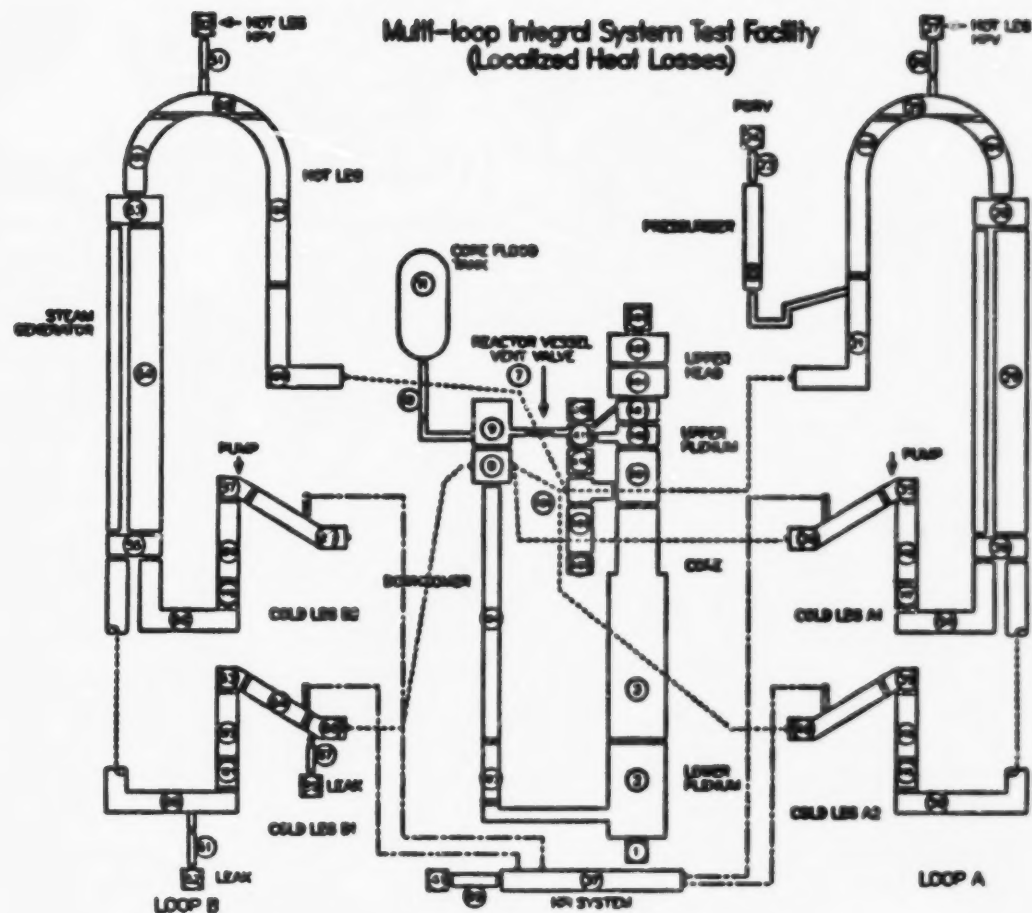


Fig. 2.
TRAC component noding schematic.

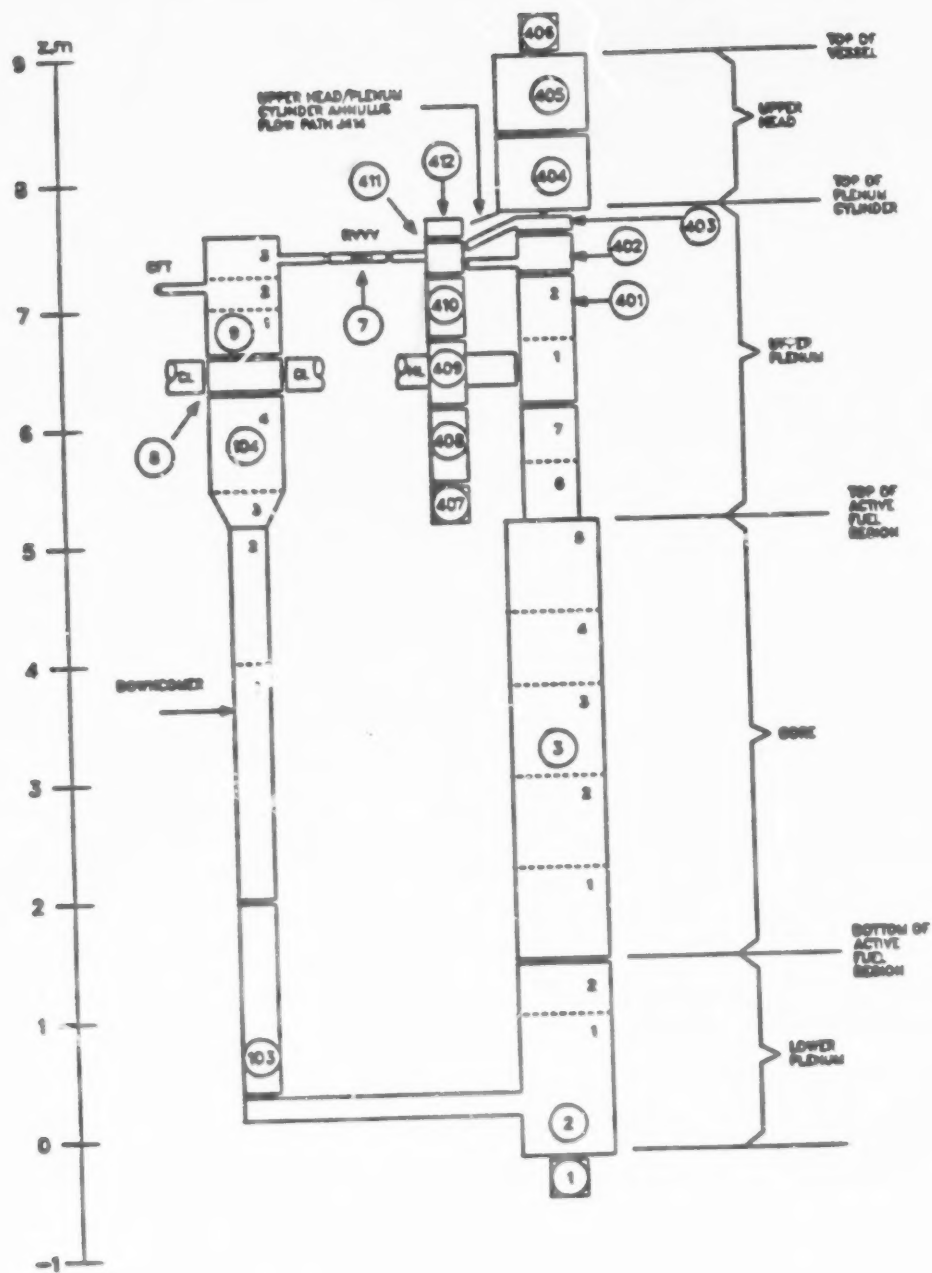


Fig. 3.
TRAC reactor vessel noding schematic.

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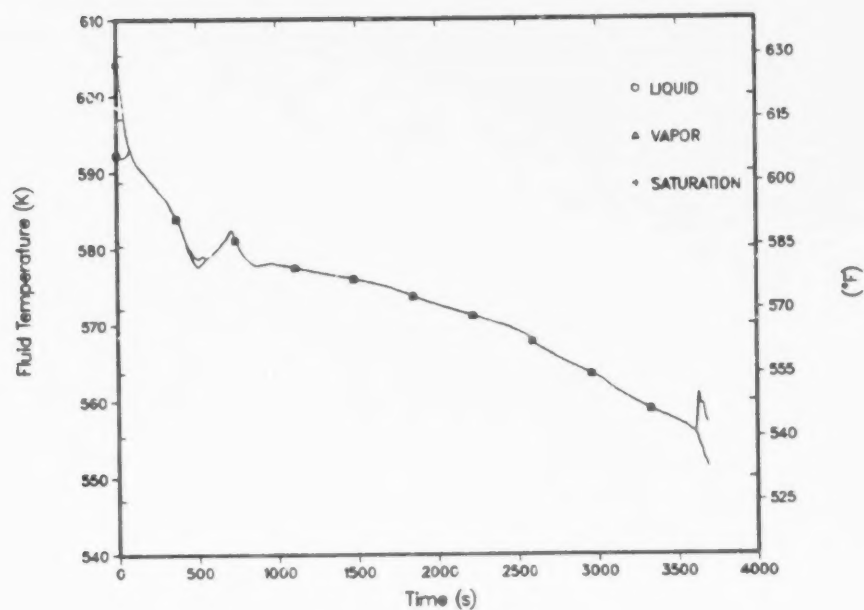


Fig. 4.

TRAC calculation for Test 310000, temperatures in intact-loop hot-leg U bend.

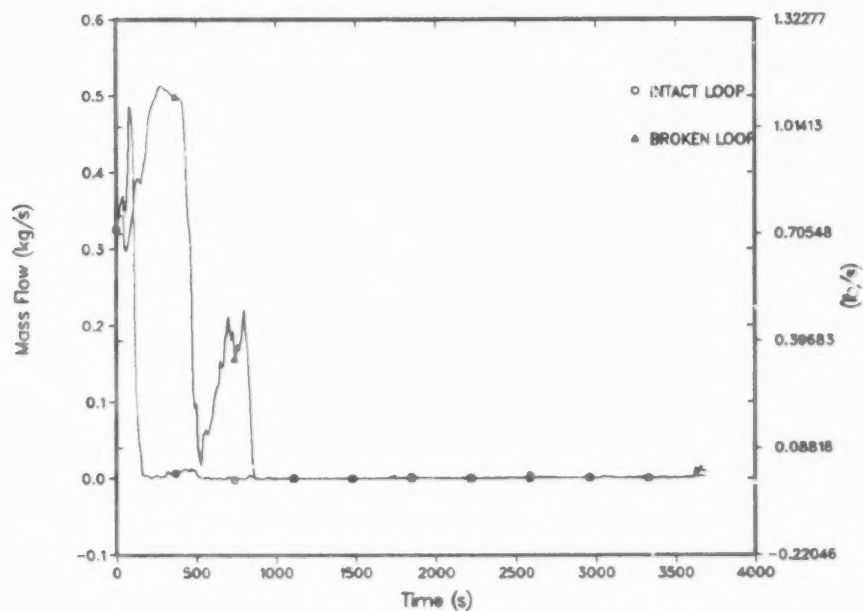


Fig. 5.

TRAC calculation for Test 310000, hot-leg U-bend mass flows.

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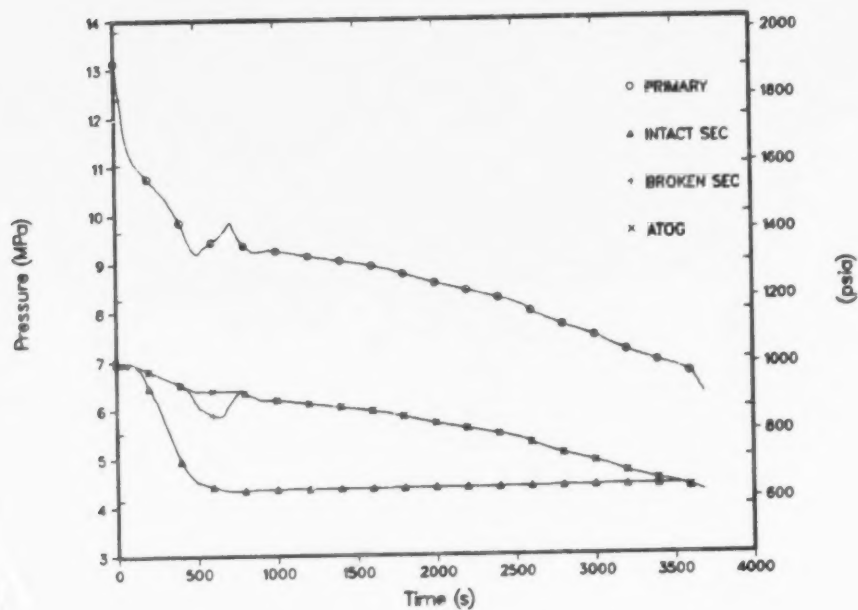


Fig. 6.

TRAC calculation for Test 310000, primary and secondary system pressures.

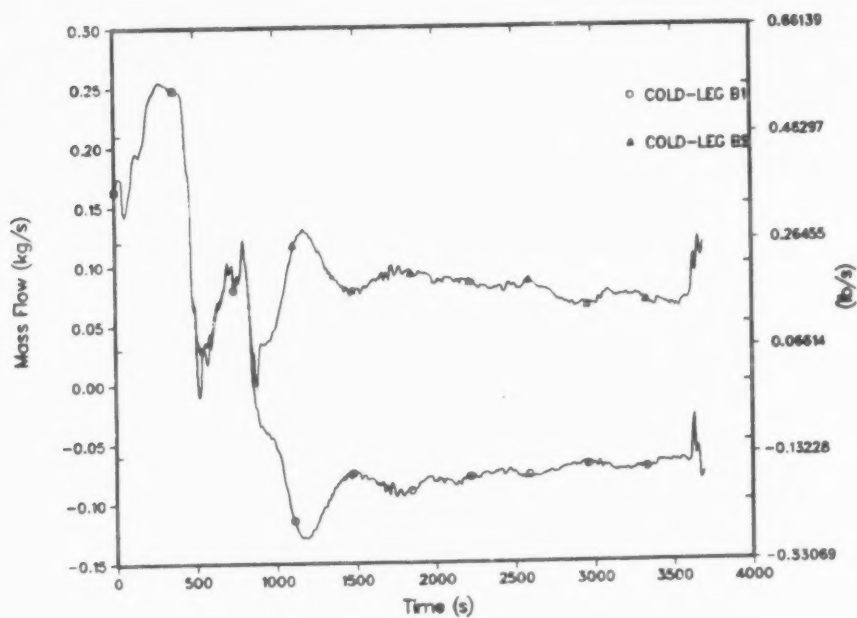


Fig. 7.

TRAC calculation for Test 310000, broken-loop cold-leg pump-suction mass flows.

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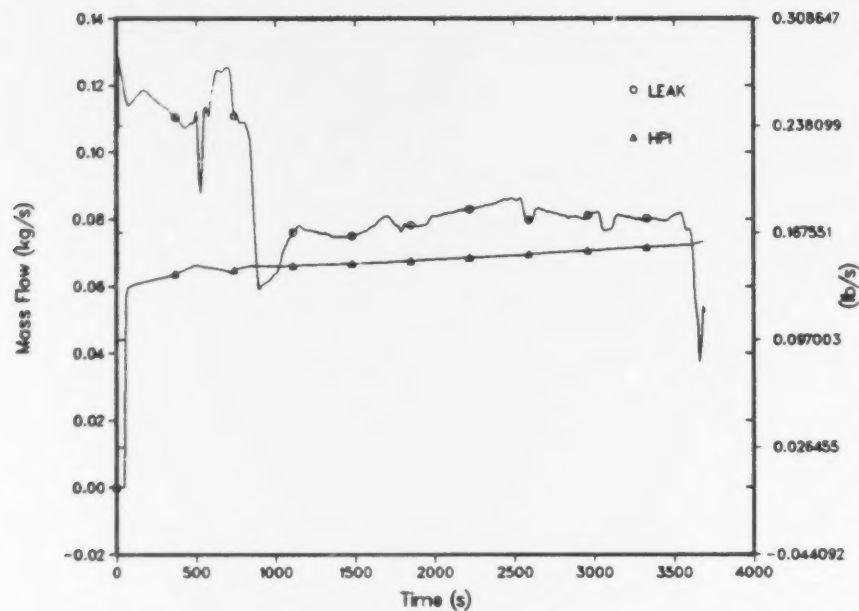


Fig. 8.

TRAC calculation for Test 310000, cold-leg leak and HPI flows.

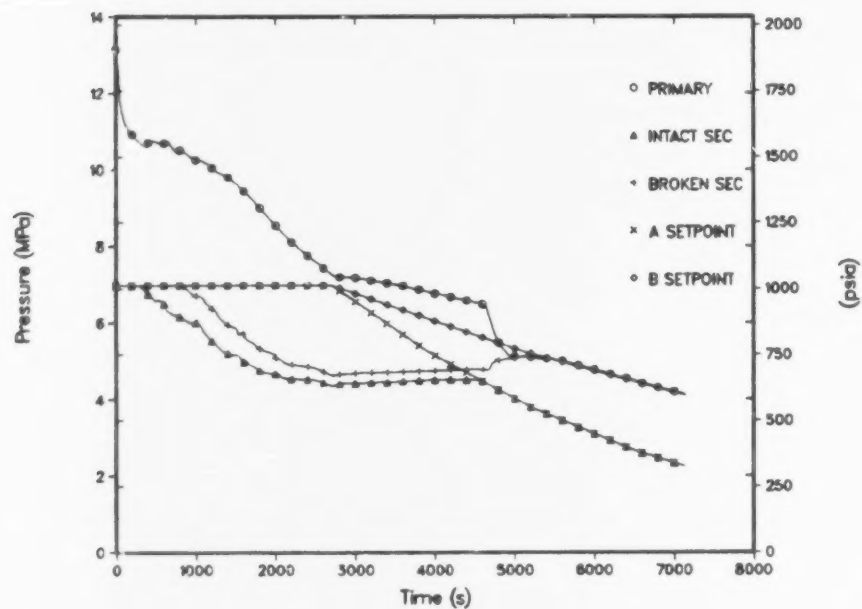


Fig. 9.

TRAC calculation for Test 310503, primary and secondary system pressures.

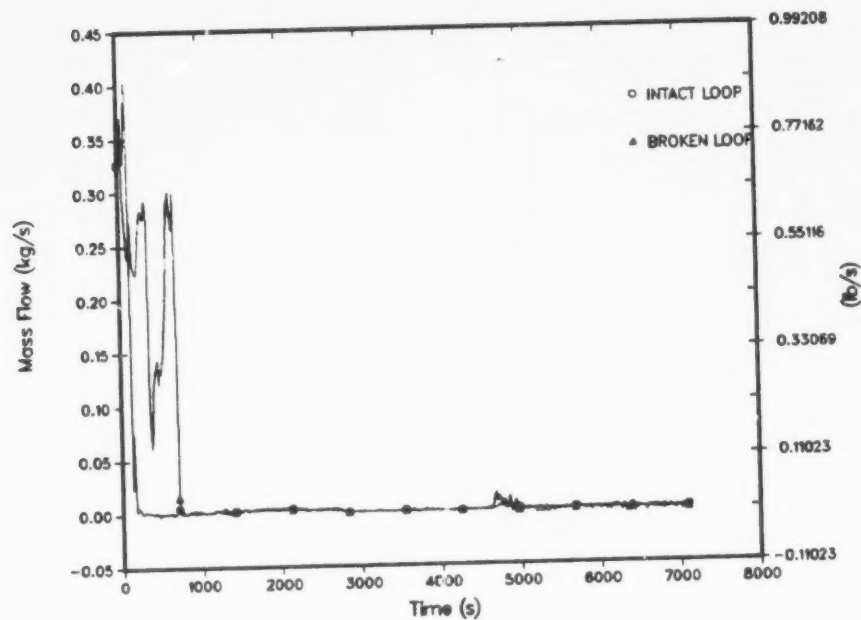


Fig. 10.

TRAC calculation for Test 310503, hot-leg U-bend mass flows.

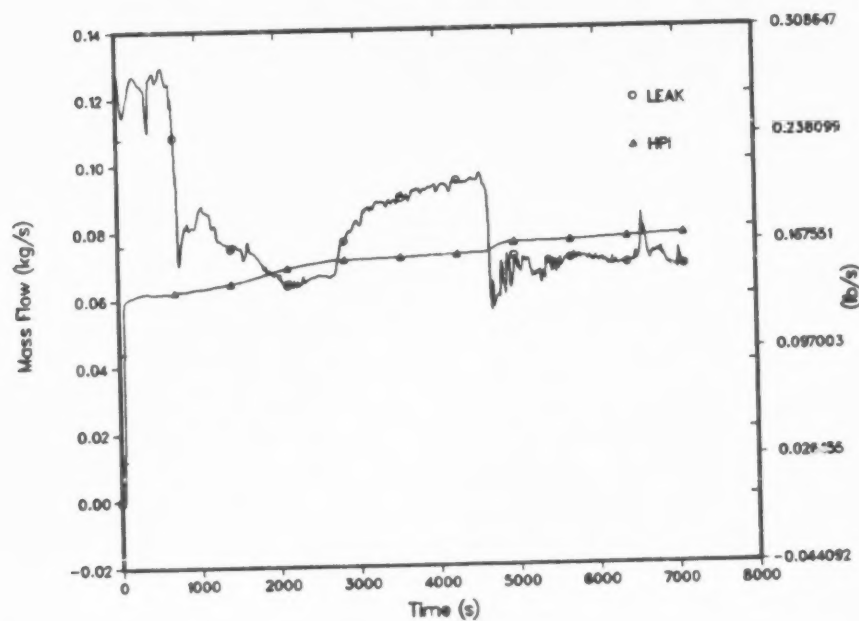


Fig. 11.

TRAC calculation for Test 310503, cold-leg leak and HPI flows.

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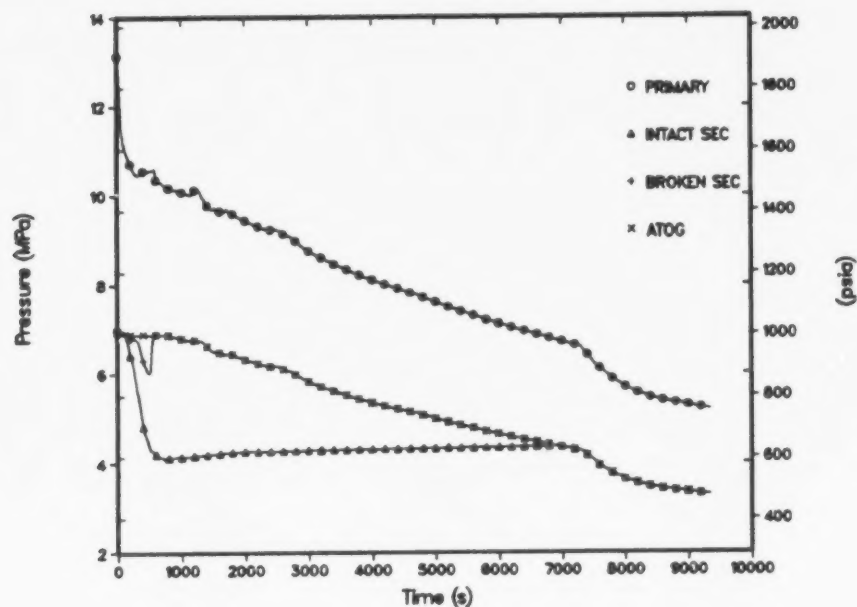


Fig. 12.

TRAC calculation for Test 320604, primary and secondary system pressures.

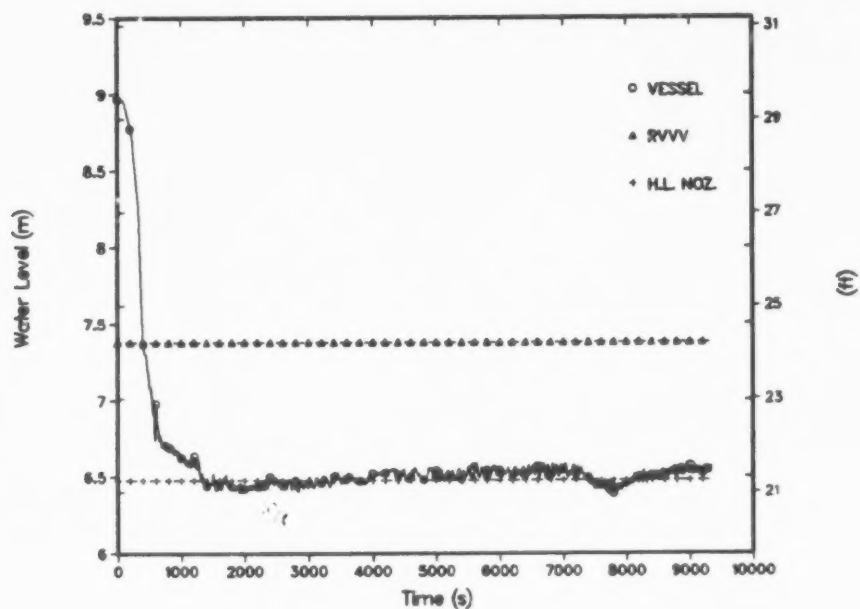


Fig. 13.

TRAC calculation for Test 320604, reactor vessel collapsed liquid level.

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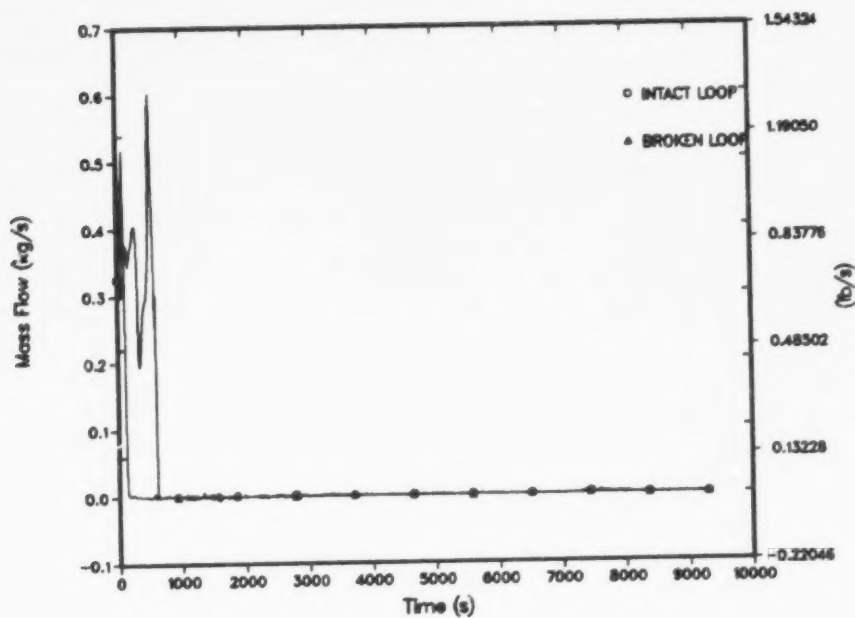


Fig. 14.

TRAC calculation for Test 320604, hot-leg U-bend mass flows.

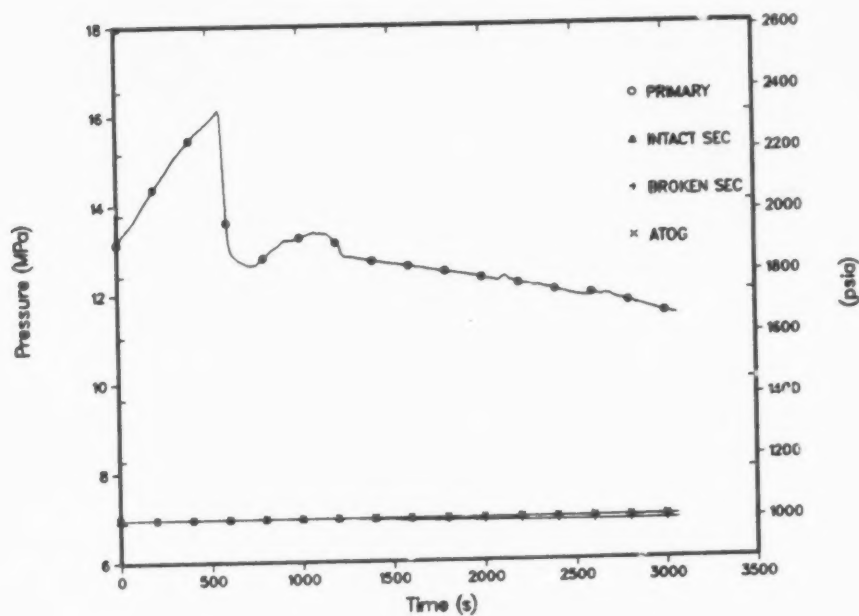


Fig. 15.

TRAC calculation for Test 330201, primary and secondary system pressures.

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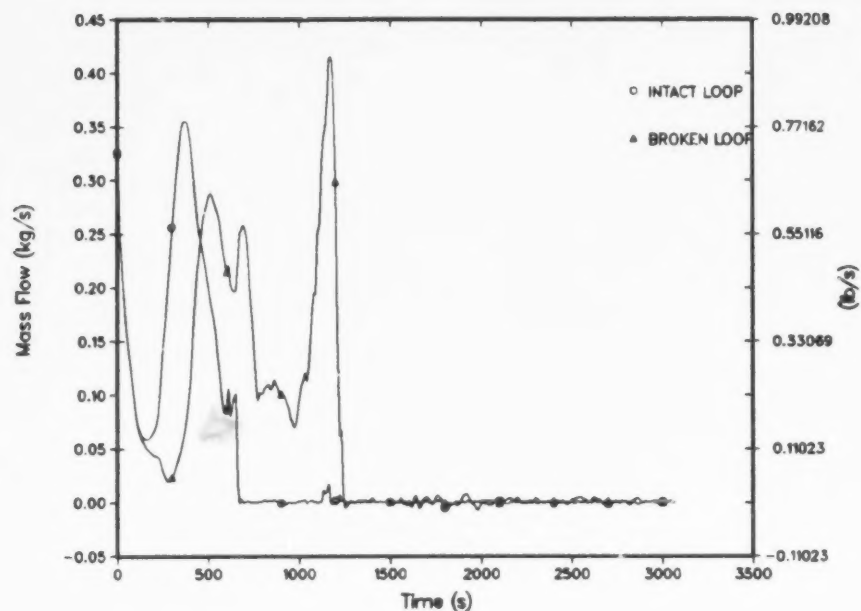


Fig. 16.

TRAC calculation for Test 330201, hot-leg U-bend mass flows.

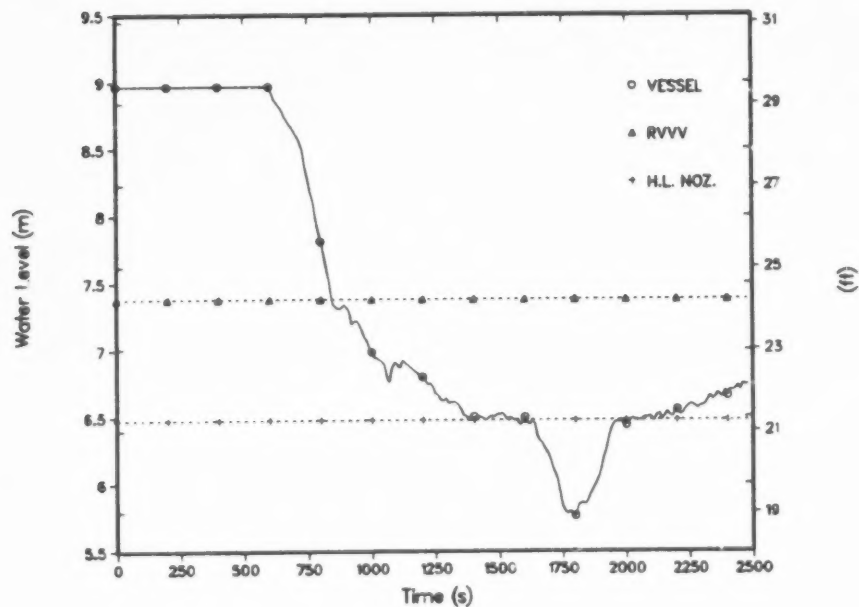


Fig. 17.

TRAC calculation for Test 330302, reactor vessel collapsed liquid levels.

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SMALL BREAK LOSS-OF-COOLANT TEST RESULTS
FOR UMCP 2X4 LOOP ONE AND TWO-PHASE
NATURAL CIRCULATION IN MULTI-LOOP SYSTEM

by

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1.0 Introduction

The University of Maryland at College Park (UMCP) 2x4 Loop has been operational since 1985. During the last year, many single-phase natural circulation tests were conducted. After the single-phase test series were completed, the facility was modified to allow for better instrumentation, for addition of cold-leg orifices simulating pump resistances, and for finer control of secondary flow rates. Since then, various phases of the transient (single-phase, two-phase, and boiling condensation mode) related to a Small Break Loss-of-Coolant Accident (SBLOCA) have been conducted.

In this report, the salient features of single-phase natural circulation, two-phase natural circulation, and boiling condensation mode (BCM) test results are presented and the observed phenomena at the UMCP 2x4 Loop are discussed.

2.0 Loop Description

The UMCP 2x4 Loop is a 1/500 volume scaled model of an B&W lowered loop design, short in height and proportionally compensated in cross-sectional area (Figure 1). The loop is constructed entirely of stainless steel and operates at a maximum pressure of 300 psia. Power of a maximum of approximately 200kW, is provided by 15 electrical heater rods.

The facility consists of a reactor vessel with core barrel and annular downcomer, two Once Through Steam Generators (OTSGs), two hot legs, four cold legs, and a pressurizer. The core barrel is equipped with eight flapper reactor vessel vent valves (RVVVs).

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The secondary side of the OTSG provides a large amount of flexibility in the positioning of the thermal center. For example, if desired, only the bottom half of the steam generator can be cooled, or only the top half.

Auxiliary systems in the loop include high pressure safety injection, auxiliary feedwater, and blowdown collection system.

The loop is instrumented with over a 100 thermocouples, absolute and differential pressure transducers, and flow meters. Data is currently collected and stored with a microcomputer based data acquisition system.

Detailed description of the facility can be found in Reference 1.

3.0 Objective

The detail investigation of SBLOCA phenomena is the main focus of the current testing at the UMCP 2x4 Loop. These tests include the study of the following three items.

- (a) Hot Leg - phase separation, flow termination and resumption, intermittent flow.
- (b) RVVV - open/close behavior during system blowdown.
- (c) OTSG behavior - relationship between the primary and secondary sides; how secondary side level, flow, and etc., affect the primary side behavior such as flows, pressure, etc.)

At present, the first and third items have been studied. RVVV behavior will be investigated in future experiments.

4.0 SBLOCA Sequence

The events encountered during a SBLOCA are controlled by many parameters, and the sequence can vary widely for various situations. But in general, the whole transient could be breakup into three parts. They are,

- (a) Rapid Depressurization
- (b) Flow Equalization
- (c) Stable Boiling Condensation Mode

4.1 Rapid Depressurization

During this part of the SBLOCA transient, the loop is subcooled and the void is present only in the pressurizer. Flashing in the hot leg will occur when the pressurizer empties, and these two events will determine the end of this first phase. A

simplistic model was proposed to scale this part of the transient based on system parameters and boundary conditions (e.g. decay power level, break size, etc.).

4.2 Flow Equilization

The flashing in the hot leg and the complete drainage of the pressurizer mark the beginning of this part of the SBLOCA transient. The High Pressure Injection (HPI) pump will start to inject coolant at a prescribed flow rate. Interruption and resumption of natural circulation will be observed and BCM will occur in one of the steam generators. The flow equilization between the HPI and the break will determine the end of this transient.

5.0 Single-Phase Natural Circulation

The single-phase natural circulation tests were run with the purpose of,

- (a) to check the commonly used natural circulation equation [Reference 2] for a two loop system (loop A and B) in turbulent flow. That is,

$$W_{HL} = \left[\frac{28 \rho^2 g l_{TH} q_c}{C_a (R_a + 4R_v)} \right]^{1/3} \quad (1)$$

where β : volumetric expansion coefficient,
 ρ : density,
 g : gravitational acceleration,
 l_{TH} : thermal length,
 q_c : core power,
 C_a : specific heat at loop A average temperature,
 R_a : hydraulic resistance of loop A (one hot leg, steam generator, and two cold legs),
 R_v : hydraulic resistance of vessel (downcomer, lower plenum, core, and upper plenum),

- (b) to study the two loop behavior when the load on the secondary side of the steam generators is out of balance, and
- (c) to determine the stability of the system when the secondary side of the OTSG undergoes transients.

The results of the analytical and experimental results are summarized as follows:

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5.1 The Natural Circulation Equation

In equation (1), W_{HL} is not a simple $1/3$ power of q_c since both resistance, R 's and thermal length, l_{TH} varies with flow. Resistance is a function of Re , but it is a weak function during turbulent flow. The thermal length, l_{TH} , on the other hand, is a fairly strong function of W_{HL} .

The thermal centers are determined from density distribution, which in turn, are functions of temperature. Starting with counter-current heat exchanger solution, the thermal center can be calculated, in terms of secondary side flow rate (W_s) and core power (q_c) and the relationship of l_{TH} as a function of W_s and q_c is shown in figure 2. Thus, the dependency of W_{HL} in equation (1) is a weaker function of q_c if W_{HL} were expressed in real explicit form.

In general, the thermal length, in single phase flow, depends upon thermal load and secondary side inlet temperature. An increase in core power, on the other hand, would increase the thermal length due to the increase in temperature gradient along-side the heat exchanger.

5.2 Response of the Primary System to Asymmetric Steam Generator Load

Asymmetric distribution of secondary side flow rate results in similar primary side response. Higher secondary side flow rate extracts more energy in the corresponding heat exchangers, enhancing the rate of heat transfer, which in turn, increase the rate of primary side flow rate and lowers primary side temperature gradient. However, the magnitude of primary side flow redistribution is less drastic as compared to the secondary side flow rates (figures 3 and 4). This is due to the fact that the thermal length of the loop with higher secondary flow rate decrease while friction head loss increase. The reverse is true with lower secondary side flow rate. The net result would be a milder response of the primary side to the imposed asymmetry.

5.3 Stability of the Primary System Flow during Imposed Transient

Flow instability resulting in cessation of circulation was not observed either in the asymmetry or during two-to-one loop operation transients. However, flow and temperature fluctuations existed in the loops with lower secondary-side flow rates. The magnitude of these fluctuations increased with a decrease in the rate of secondary-side flow (figure 5). This implies that the loop flow resistance intrinsically damps oscillations. However, primary system temperature increase due to asymmetric operation (figure 6). The system average temperature rose during a symmetric to asymmetric transient while the total secondary-side flow rate was maintained. This is due to the fact that the rate of

heat transfer enhancement in the loop having higher flows.

A simplified but adequate mathematical modeling was developed based on, (a) lumped, steady state log-mean temperature difference and an overall heat transfer coefficient in heat exchangers, (b) an overall mass balance for the case of two-loop, and (c) loop integral momentum equation. Physical properties of subcooled water was assumed to be independent of pressure. This assumption proved to be adequate for predicting the system response to transients. A modified two-phase flow computer program has been developed, which is a versatile, faster than real time code capable of predicting pertinent parameter of primary system and pressurizer under operating transients and events such as SBLOCA and loss of primary system pumps.

6.0 Boiling Condensation Mode

Phenomenology

Three tests were conducted to identify phenomena occurring during gradual loss of primary inventory through a small break. The tests share basic features defining loop conditions. For all the tests, the liquid level in the secondary is fixed at mid level, and the primary inventory is gradually drained with no makeup water to the system. However, the operational procedures for the three tests are quite different. Test 1 (BCM0918) is controlled by power modulation, for Test 3 (BCM0920), the power is maintained constant and pressure control is achieved by actuating the Power Operated Relief Valve (PORV). Test 2 (BCM0919) modulates power and include periods during which the leak from the primary is isolated. It is used to establish steady state conditions at different inventory levels.

Surprisingly, in spite of large variation of operating procedures, all tests displayed similar operational regimes. In order of decreasing primary system inventory, the following four characteristic operational regimes can be identified.

- (1) Continuous natural circulation: Initially, single phase and subsequently, as primary system inventory is depleted, two-phase natural circulation. Excellent primary to secondary heat transfer.
- (2) Intermittent two-phase flow: A flow regime having complex but consistent and reproducible characteristics. Energy removal capability is somewhat degraded but still quite good.
- (3) "Frozen" flow regime: As inventory is depleted further, the intermittent two-phase flow regime stops abruptly. This occurs while the primary liquid level is still above the liquid level in the secondary. Two-phase flow termination produces a significant degradation of energy removing capability.

bilities. Operator action is required to control rising pressure.

- (4) Steady State BCM: Further loss of primary system inventory uncovers condensing surfaces in the steam generator. Core energy is now carried to the steam generator by vapor. Primary mass flow rates are therefore reduced, but energy removal capability is high and is comparable to the continuous natural circulation regime (regime 1).

As noted, the four operational regimes described above were observed for all three tests. Figures 7 and 8 provide an overview of some of the characteristics of the four operational regimes. Figure 2 shows that pressure transient for test BCM0918. Note that the power (shown in figure 9) remains constant up to 50 minutes. The pressure rise up to this time is generated by transition of operational regimes from 1 (continuous natural circulation) to 3 ("frozen" flow). The regimes are identifiable by both the pressure behavior and the difference between the inlet and outlet temperatures of the secondary which measure the heat sink effectiveness. Out to 18 minutes, continuous natural circulation prevails. Intermittent two-phase flow now starts and continues till 48 minutes. At this time, as the converging secondary temperatures show, the heat sink is practically lost and primary pressure increases rapidly. Pressure is controlled only by a drastic reduction in power. In the next 25 minutes, the heat sink is recovered and power can again be increased.

The same general phenomena are shown in figure 8. As noted, for this test the power is maintained at a constant level. When the "frozen" flow regime is encountered, the pressure rise is controlled by periodic venting of the PORV. The venting leads to additional primary system inventory loss, and as a consequence the duration of the "frozen" flow period for this test is shorter. Note that at about 50 minutes into the run an aborted attempt of transition from the intermittent two-phase flow to "frozen" flow seems to have occurred. A detailed study of the cause of this is currently being undertaken.

6.1 Continuous Natural Circulation

This operational regime is characterized by a very high primary system inventory. The water flows over the hot leg U-bend continuously, although some void is observed. Heat transfer is effective in spite of the lowered liquid level of the secondary side steam generator. As void increases, some asymmetries are observed in the cold legs but both loops contribute actively to the heat removal from the core. At some point the inventory level is reduced to the extent that circulation is interrupted. Since the pressurizer empties, the subcooling effects due to the pressurizer loop seal fluid are not present.

Figure 10 shows that the flow is interrupted in both legs at the same time and that the level in both hot legs collapsed simultaneously.

6.2 Intermittent two-phase flow

The subsequent operational regime is characterized by fair heat transfer from the core to the steam generators. This regime is very oscillatory. A few observations regarding this rather complex phenomena can be made. The first important clue is obtained by considering figure 11. When flow towards the core from the hot legs is observed, pressure in the vessel is decreasing. This demonstrates conclusively that the driving force of the oscillation is generated within the vessel, more specifically, by a condensation phenomena occurring within the vessel itself. At this time it is difficult to give a substantiated explanation of the initiating mechanism since the current data acquisition frequency is limited. However, it is clear that subcooled water enters the core and causes condensation of steam in the upper plenum. The resulting pressure reduction exerts a strong pull on the liquid both in the hot and cold legs. The subcooled water is subsequently brought to saturation, vaporization takes place, the steam dome again expands forcing the liquid level downward until the vapor escapes to the hot legs entraining liquid over the U-Bend and causing water to flow from the core to the steam generator. This water is responsible for most of the heat transfer during this intermittent BCM period. The slug of subcooled water in the cold legs originates at the bottom of the steam generator. The cold water then pulled toward the vessel during a condensation cycle. The progress of such a cold water slug can be identified in figures 12 and 13. The thermocouple near the bottom of the steam generator (T_3) registers a fairly constant low temperature. Thermocouples T_2 and T_1 are located close to the midpoint of the cold leg and near the vessel. In figure 12, they measure the periodic sweep of cold water to the core and the return of hot water as the collapsed steam dome in the vessel is registered by temperature measurements in the fuel. It is therefore evident that the water is transported through the downcomer and rises through the core heated length in a fairly segregated slug.

Figure 10 illustrates another important feature of this operating regime. The first condensation pulse is shown to be in phase for both loops. Subsequently, off-sets of levels in the south and north loop develop indicating that an interloop oscillation is present. At about 55 minutes into the run, (not shown on figure) a strong condensation event takes place which succeeds in bringing the two loops in phase again. This event corresponds to the brief pressure rise shown at 55 minutes in figure 8.

The primary system inventory is decreasing throughout this period and at some point liquid is not entrained any more over the top of the U-Bend. The oscillations decay thereafter, and, as shown in figure 14, pressure and therefore saturation tempera-

ture increase rapidly. It was already noted in figure 7, that the exit temperature of the secondary water drops, indicating a loss of heat sink. This confirms that the heat transfer was carried out primarily by the liquid water. It seems reasonable to assume that the criteria for the termination of the intermittent two-phase flow depends on counter-current flow limitation conditions achieved in the hot leg. Pressure is increasing, thus the vapor specific volume is reduced, and simultaneously inventory is depleted; both effects lead to a failure of carry-over of fluid over the U-Bend.

6.3 "Frozen" Flow

When there is no liquid available to push the steam generator water towards the vessel, the oscillations characterizing the previous phase subside. The thermal center is inverted and very little heat is transferred to the steam generators. Consequently, the pressure increases rapidly and either the PORV must be actuated or the power must be drastically reduced. The liquid inventory continues to decrease but as long as the secondary side steam generator remains below the level of the primary, heat transfer is small. When the primary side level in the steam generator finally reaches the level of the secondary side, energy transfer is reestablished. The primary pressure rapidly decays, and the secondary inlet and outlet temperatures diverge, demonstrating that the heat sink is recovered as shown in figure 15.

6.4 Steady State BCM

As the primary inventory continues to deplete, the steam generator primary level decreases below the level of the secondary. Therefore, additional heat transfer surface is available for condensation. The heat transfer is very effective and the vapor flow in the hot legs increases. The horizontal sections of the hot legs have a very high void and later only vapor is flowing in them. When additional inventory is lost, the heater rods begin to uncover and the experiment is terminated. The increase in the energy removal capability for this operational regime is significant. The sink fully recovers the magnitude it had during the initial continuous natural circulation.

7.0 Summary and Conclusion

This paper presents experimental results obtained from the UMCP 2x4 Loop in the areas of single-phase natural circulation, two-phase natural circulation and boiling condensation mode circulation. The important findings can be summarized as follows:

- (a) For single-phase flow, the flow-power dependency was not of $1/3$ power relationship. The dependency was weaker due to the fact that the thermal center elevation difference and loop resistance were both function of power and flow.

- (b) The imbalance in the secondary side between the two steam generators produce a correspondent but less dramatic unbalance of flow in the primary side.
- (c) During transient condition, no instabilities were observed.
- (d) During the SBLOCA tests, after void was generated, four distinct operational regimes were observed. There were:
 - (i) Continuous two-phase natural circulation with good heat transfer.
 - (ii) Intermittent flow after the U-Bend was voided, with fair heat transfer.
 - (iii) "Frozen" flow with almost stagnant loop condition, and the heat transfer was very poor.
 - (iv) Steady state BCM, with good heat transfer.Stage (iii) represents a regime which should be avoided for safe operation of a reactor.
- (e) The operational regimes were observed under various test conditions. The transition from one regime to another seemed to governed by the inventory in the primary loop, the relative height of thermal centers in the primary side and the secondary side.
- (f) Experimental data seemed to indicate the existence of counter-current in the hot leg, including the counter-current flow (of hot and cold liquid) in the cold leg, counter-current flow limit in hot legs, and the initiation of flow toward vessel dome due to condensation.

References

- [1] Y. Y. Hsu, et. al., "Final Design report for the UMCP 2x4 B&W Simulation Loop," Department of Chemical and Nuclear Engineering, University of Maryland.
- [2] Y. Y. Hsu, et. al., "UMCP 2x4 Loop Test Facility Annual Report 1985," Department of Chemical and Nuclear Engineering, University of Maryland.

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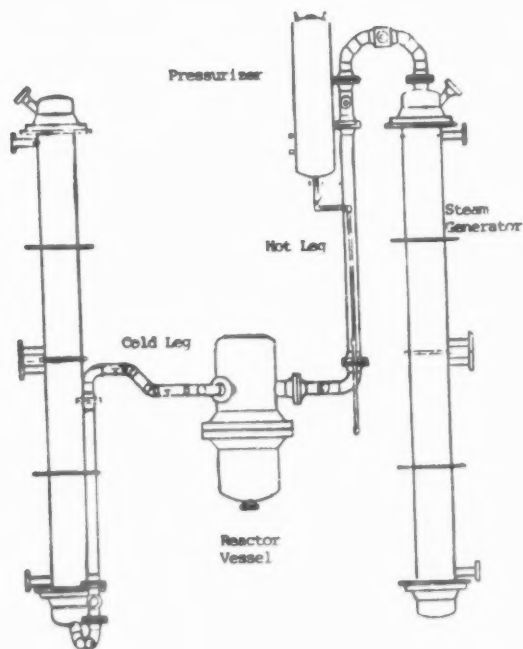


Figure 1. UMCP 2x4 B&W Loop

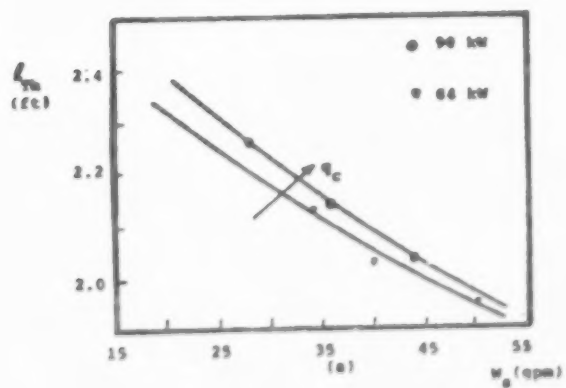


Figure 2. Variation of System Thermal Length vs. Secondary-side Mass Flow Rate

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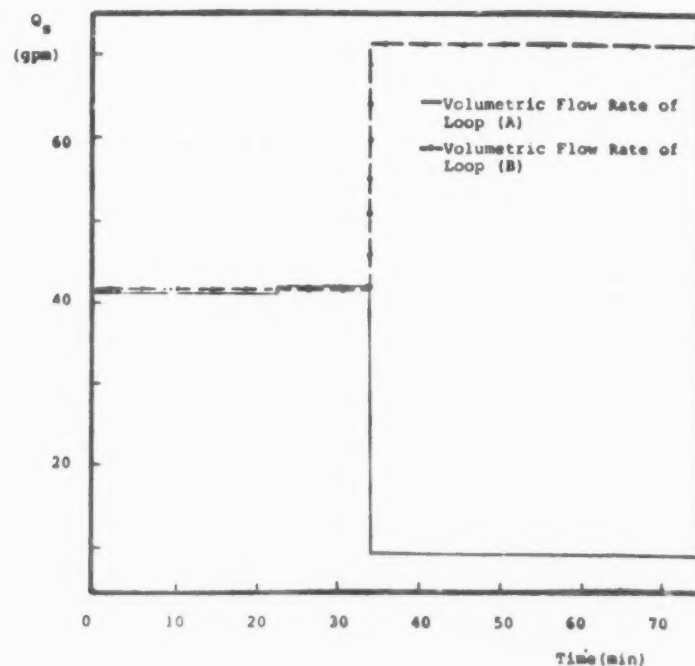


Figure 3. Seconadry-Side Volumetric Flow Rate vs. Time for Asymmetric Transient Test

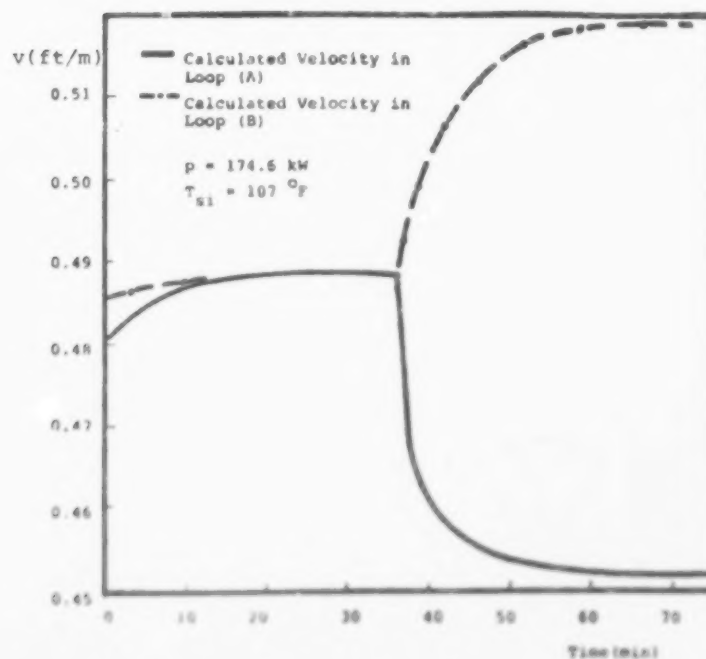


Figure 4. Hot Legs Velocity vs. Time for Asymmetric Transient Test

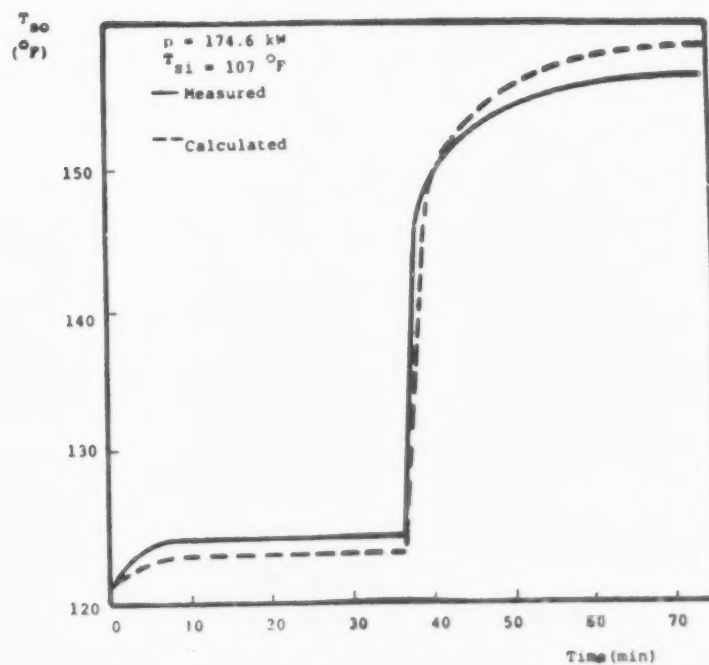


Figure 5. Secondary-Side Outlet Temperature vs. Time for Asymmetric Transient Test

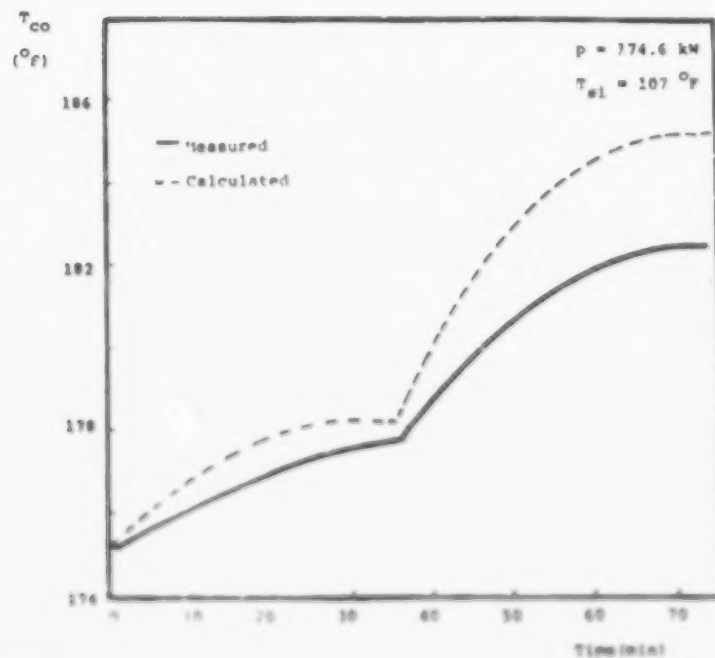


Figure 6. Upper Plenum Temperature vs. Time for Asymmetric Transient Test

BCM0918 TEST.

Press. and Secondary (South loop) Temp.

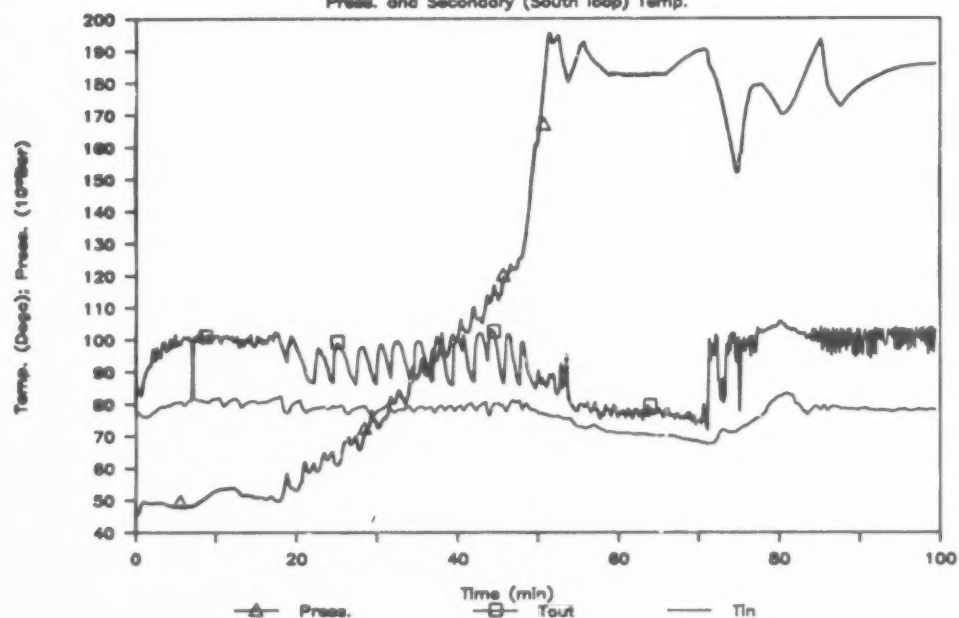


Figure 7. Primary Pressure and Secondary Inlet and Outlet Temperatures vs. Time

TEST BCM-09-20. CONST. POWER

Levels in St. Gen. (prim) & Press.

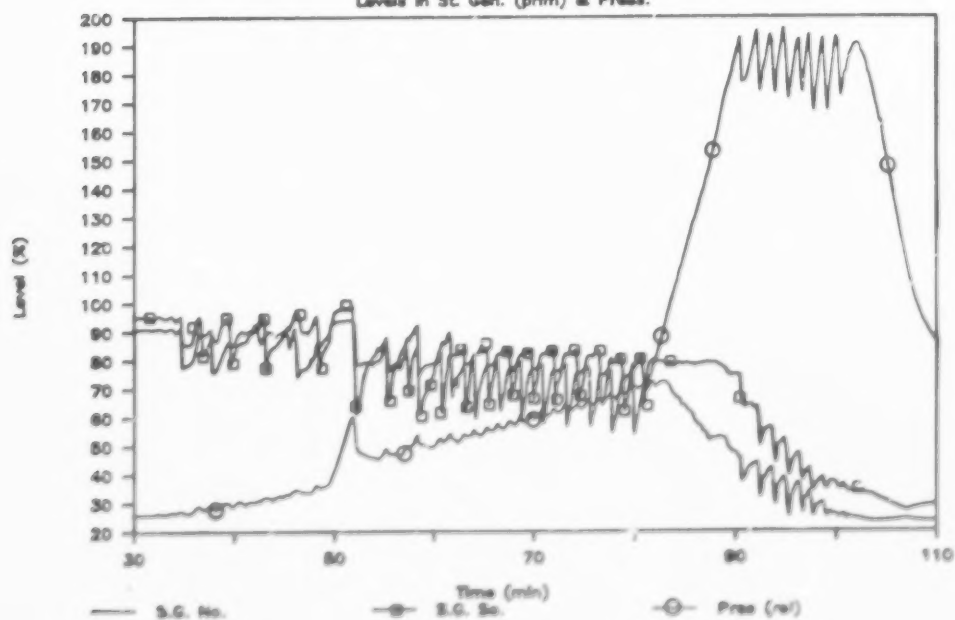


Figure 8. Primary Levels in Steam Generators and Primary Pressure vs. Time

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BCM0918 TEST.

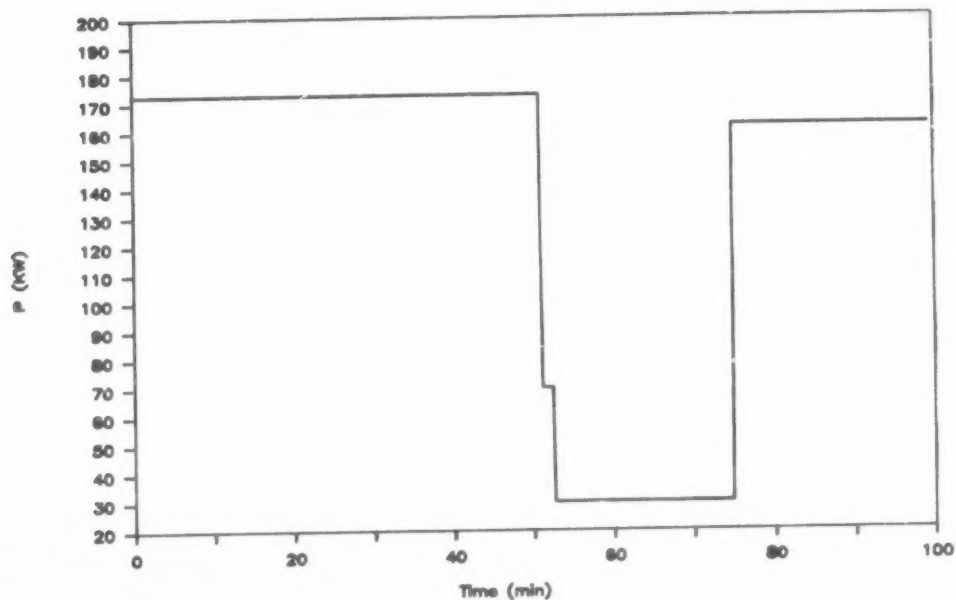


Figure 9. Power vs. Time

BCM0918 TEST.

Liquid Lev. in Hot Lg. & S.Gen.

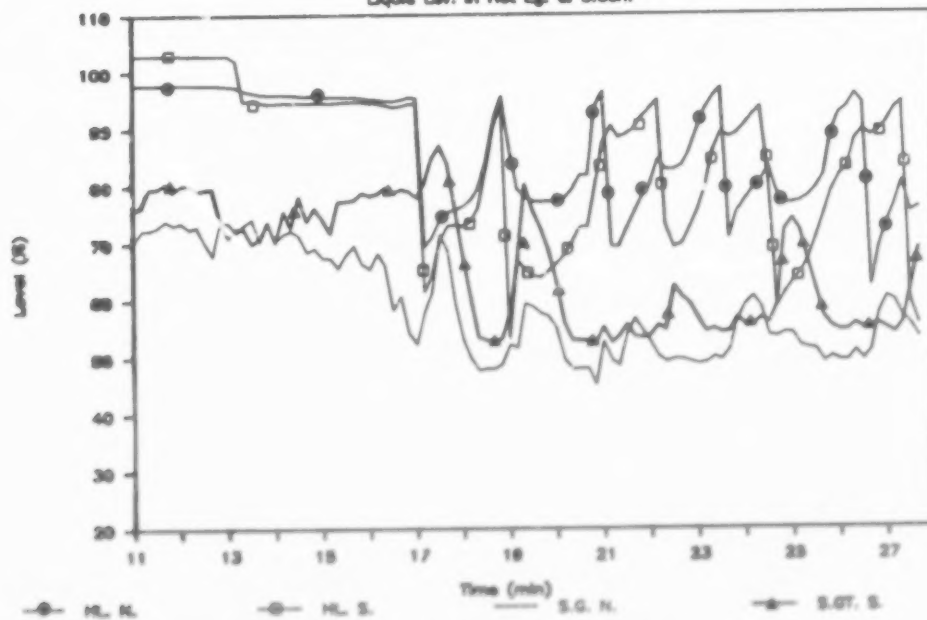


Figure 10. Liquid Level in Steam Generators and Hot Legs vs. Time

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BCM0918 TEST.

Liquid Lev. in RPV & Hot Lg.

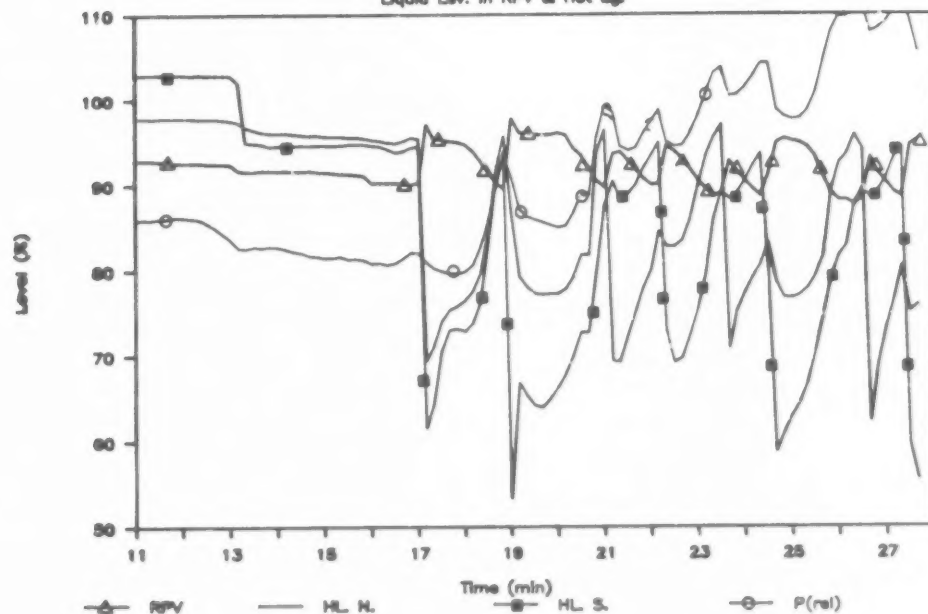


Figure 11. Liquid Level in Hot Legs and Reactor Vessel vs. Time

TEST BCM-09-20. CONST. POWER

Temp. in North East Cold Leg

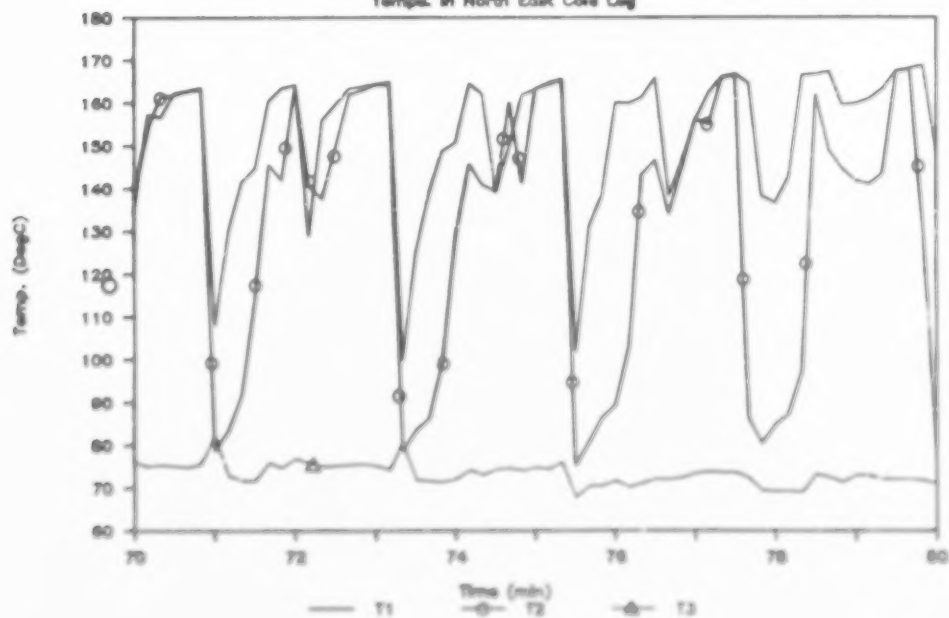


Figure 12. Temperatures in Cold Leg vs. Time

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TEST BCM-09-20. CONST. POWER

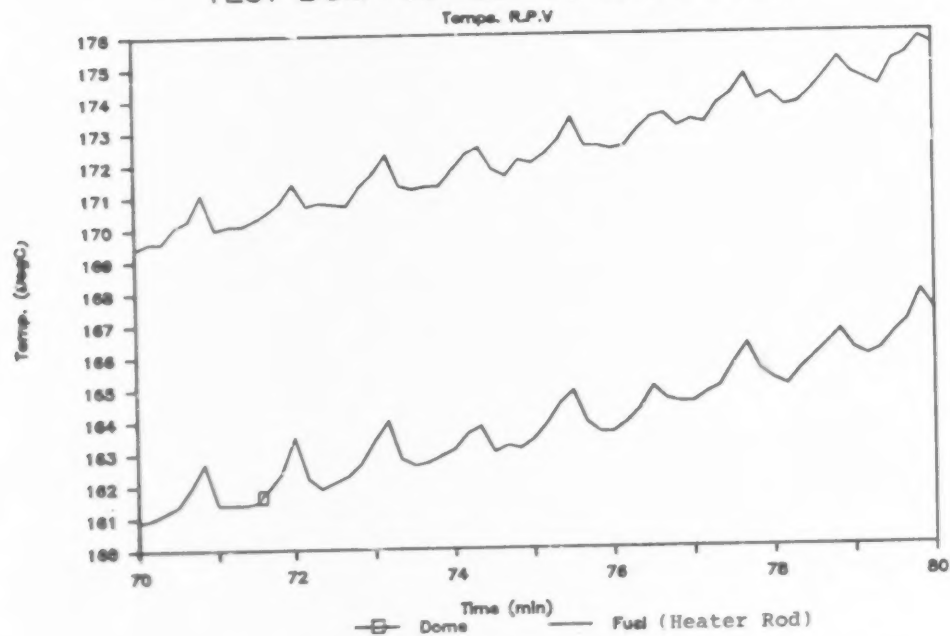


Figure 13. Dome and Heater Rod Temperatures vs. Time

TEST BCM-09-20. CONST. POWER

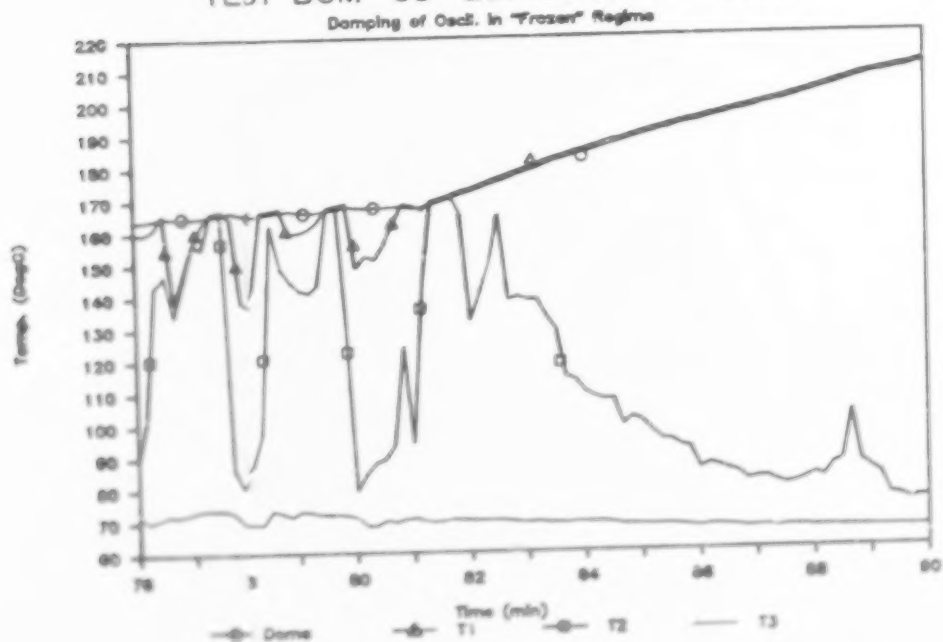


Figure 14. Temperatures of Dome and Cold Leg vs. Time

TEST BCM-09-20. CONST. POWER

Recovery of Heat Sink Temps in Sec.

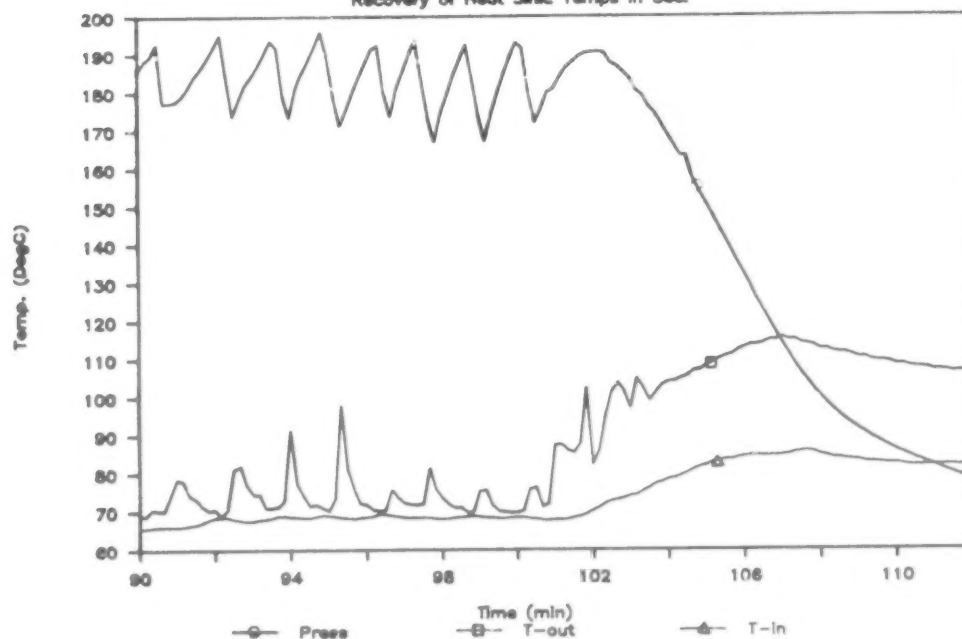


Figure 15. Primary Pressure and Secondary Inlet and Outlet Temperatures vs. Time

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SEMISCALE RECOVERY INVESTIGATIONS:
A COMPARISON OF RESULTS FROM SEMISCALE MOD-2C
SMALL BREAK LOCA WITHOUT HPI TESTS^a

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ABSTRACT

Inadequate core cooling conditions can result in small break loss-of-coolant accidents (SBLOCAs) without high pressure injection (HPI) unless appropriate operator action is taken. Results from the S-NH test series, performed in the Semiscale Mod-2C facility, show that for SBLOCAs below 2.1% inadequate core cooling results before the accumulator actuation pressure is reached. Various recovery operations were used in the experiments in the S-NH test series to mitigate the heater rod temperature excursions and lower the primary system pressure to the accumulator actuation pressure. These recovery operations included steam generator secondary steam-and-feed and primary coolant pump restart. In addition, in one of the experiments steam generator secondary steam-and-feed operations were begun before a heater rod temperature excursion began. The effectiveness each procedure had in mitigating the heater rod temperature excursions is discussed and compared.

INTRODUCTION

This paper presents a comparison of the results from the S-NH test series performed in the Semiscale Mod-2C facility. The S-NH test series¹ simulated small break loss-of-coolant accidents (SBLOCAs) without high pressure injection (HPI) and was designed to gain information on various recovery procedures that are outlined in the emergency operating procedures (EOPs) of commercial pressurized water reactor (PWR) plants. Experiments S-NH-1, S-NH-3, and S-NH-5 simulated a shear of a 2-in. line penetrating the cold leg of a commercial PWR plant, while Experiment S-NH-2 simulated the shear of a 4-in. line. Three different recovery scenarios were simulated to assess their effectiveness in mitigating the resulting core heater rod temperature excursions. In Experiments S-NH-1^{2,3} and S-NH-2,^{2,4} a steam generator steam- and-feed recovery was initiated when the core peak cladding temperature (PCT) reached 811 K (1000°F). In Experiment S-NH-3,^{5,6} a primary coolant pump was restarted when the PCT reached 811 K (1000°F) and the steam generator steam-and-feed operations were begun when the PCT reached 950 K (1250°F). In Experiment S-NH-5,^{7,8} the steam generator

a. Work supported by the U.S. Nuclear Regulatory Commission, Office of Nuclear Regulatory Research, under DOE Contract No. DE-AC07-76ID01570.

steam-and-feed recovery was initiated when the vessel liquid level reached the elevation of the top of the core, before a heater rod temperature excursion began.

The safety issue associated with SBLOCAs without HPI is the possibility of inadequate core cooling resulting from the loss of coolant before the primary pressure decreases to the accumulator and low pressure injection system (LPIS) pressure setpoints. If a core heater rod dry-out should occur, fuel damage may result before fluid from the LPIS can reflood the core. The emergency operating procedure for the Zion nuclear plant (EOP-11)⁹ specifies that the operator initiate steam-and-feed or pump restart recovery operations should a condition of inadequate core cooling occur. The EOP defines inadequate core cooling to exist when the average of the ten highest indicating core exit thermocouples is greater than 922 K (1200°F). EOP-11 for the Zion nuclear plant further specifies that following the unavailability or failure of both a steam generator secondary steam-and-feed operation and a primary steam-and-feed operation to cool down and depressurize the primary system, the reactor coolant pumps (RCPs) are to be restarted (one at a time) in an attempt to reestablish core cooling through either steam flow or two-phase flow. The restart of an RCP under inadequate core cooling conditions is a final effort to prevent severe core damage during a SBLOCA due to loss of other engineered safety features (HPI and auxiliary feedwater).

Concern has been expressed that the restart of a RCP in a highly voided system may not reestablish adequate core cooling. This concern is partially based on the results of a pump restart experiment (L8-2)¹⁰ performed in the LOFT facility, in which the RCPs were restarted with a voided core and failed to reverse the fuel rod thermal excursion. Another concern is whether the steam generator steam-and-feed operation applied late in the transient (after a condition of inadequate core cooling already exists) can reduce the primary system pressure to the accumulator actuation pressure.

Characteristic of all the experiments, regardless of break size, was a rapid primary system depressurization until the primary system inventory reached 75%. The primary system pressure then became very stable until the primary inventory reached 35%. The pressure at which the primary system stabilized was dependent on the steam generator secondary system pressures. With the possibility that fuel rod damage may already have occurred before recovery procedures are initiated (if the average of the ten highest thermocouples is used to detect inadequate core cooling), criteria to initiate the recovery procedure earlier based on the average pressure difference between the primary system pressure and the steam generator secondaries may be feasible.

FACILITY DESCRIPTION

Semiscale Mod-2C is a scaled model representation of a commercial four-loop PWR plant, with a fluid volume of about 1/1705 of a commercial PWR (Figure 1). The modified-volume scaling philosophy¹¹ preserves most of the first-order effects thought important for SBLOCA transients. Most

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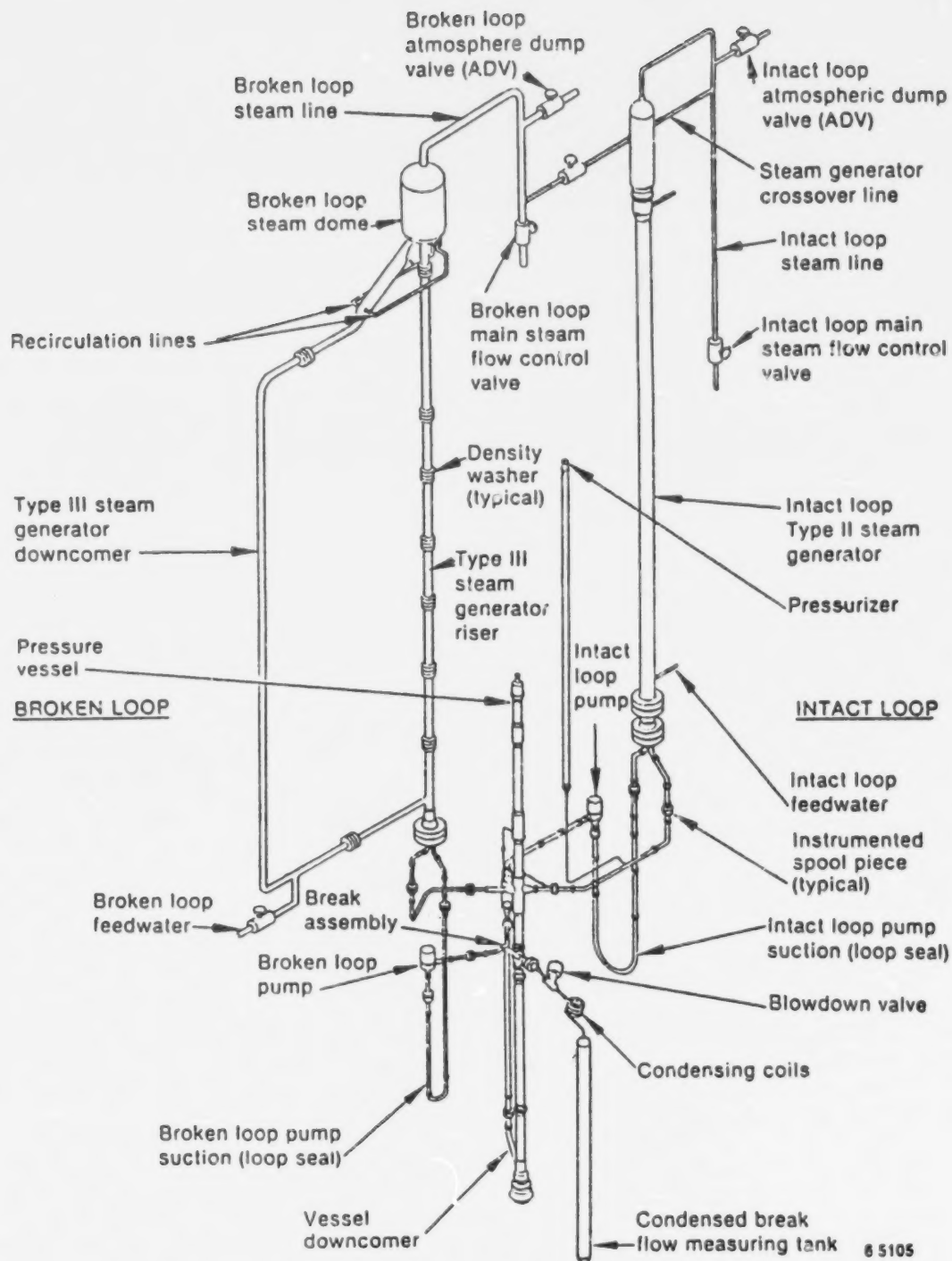


Figure 1. Semiscale Mod-2C system configured for the NH test series.

notably, the 1:1 elevation scaling of the Semiscale system is an important criterion for preserving the factors influencing signature response to a SBLOCA.

The Mod-2C system consists of a pressure vessel with external downcomer and simulated reactor internals; an "intact loop," with a shell and inverted U-tube active steam generator, pressurizer, and pump; and a "broken loop," including an active pump, active steam generator, and associated piping to allow break simulations. The intact loop simulates three loops of a four-loop PWR, and the broken loop simulates the loop in which the small break is assumed to occur. The simulated center-line, communicative breaks were 0.5% and 2.1% of the cold leg flow area and located between the pump and the vessel. The breaks were simulated by orifices that had scaled areas equivalent to 5- and 10-cm (2- and 4-in.) break diameters in a commercial PWR plant. The unique feature of the Mod-2C system is the installation in the broken loop of a Type III steam generator that includes an external downcomer, allowing use of gamma densitometers to measure the riser density at various locations. The intact loop steam generator contains six inverted U-tubes; and the broken loop contains two inverted U-tubes, both with 2.2-cm (0.86-in.) outer diameter. Vessel internals include a simulated core, consisting of a 5 x 5 array of internally heated electric rods, of which 23 were powered. The rods are geometrically similar to nuclear rods, with a heated length of 3.66 m (12 ft) and an outside diameter of 1.072 cm (0.42 in.).

The vessel upper head (Figure 2) simulates a Westinghouse inverted top hat upper head internal package design and accounts for about the top 25% of the pressure vessel volume. The Semiscale upper head contains a simulated control rod guide tube and two simulated support columns, with an upper core support plate providing the boundary between the upper head and the upper plenum. The guide tube and the two support columns penetrate through the core support plate extending into the upper plenum region. The support columns are plugged, allowing draining only through the guide tube and downcomer/upper head bypass line. The downcomer/upper head bypass line simulates leakage between these two components in a commercial PWR plant which varies between 0.5% and 4% of the total core flow. For the experiments in the S-NH test series, a flat plate orifice with a 0.594-cm (0.234-in.) diameter hole was installed in the 1.27-cm (0.5-in.) tubing bypass line. This created an initial condition core bypass flow of about 3.15% for all four experiments, within the range of the bypass flows thought to exist in commercial PWR plants. External heaters were installed in a relatively uniform manner on the vessel and loop piping to offset environmental heat loss. In addition, core power was augmented by a continuous 22 kW during the transients to make up for heat loss unaccounted for by external heaters.

Conditions in the system were monitored by an extensive network of metal and fluid thermocouples and differential pressure transducers. In the broken loop steam generator, both U-tubes were extensively instrumented with both primary and secondary-side fluid and metal thermocouples and several primary-side differential pressure transducers. Average fluid density was measured in the loops and vessel with gamma densitometers, while volumetric

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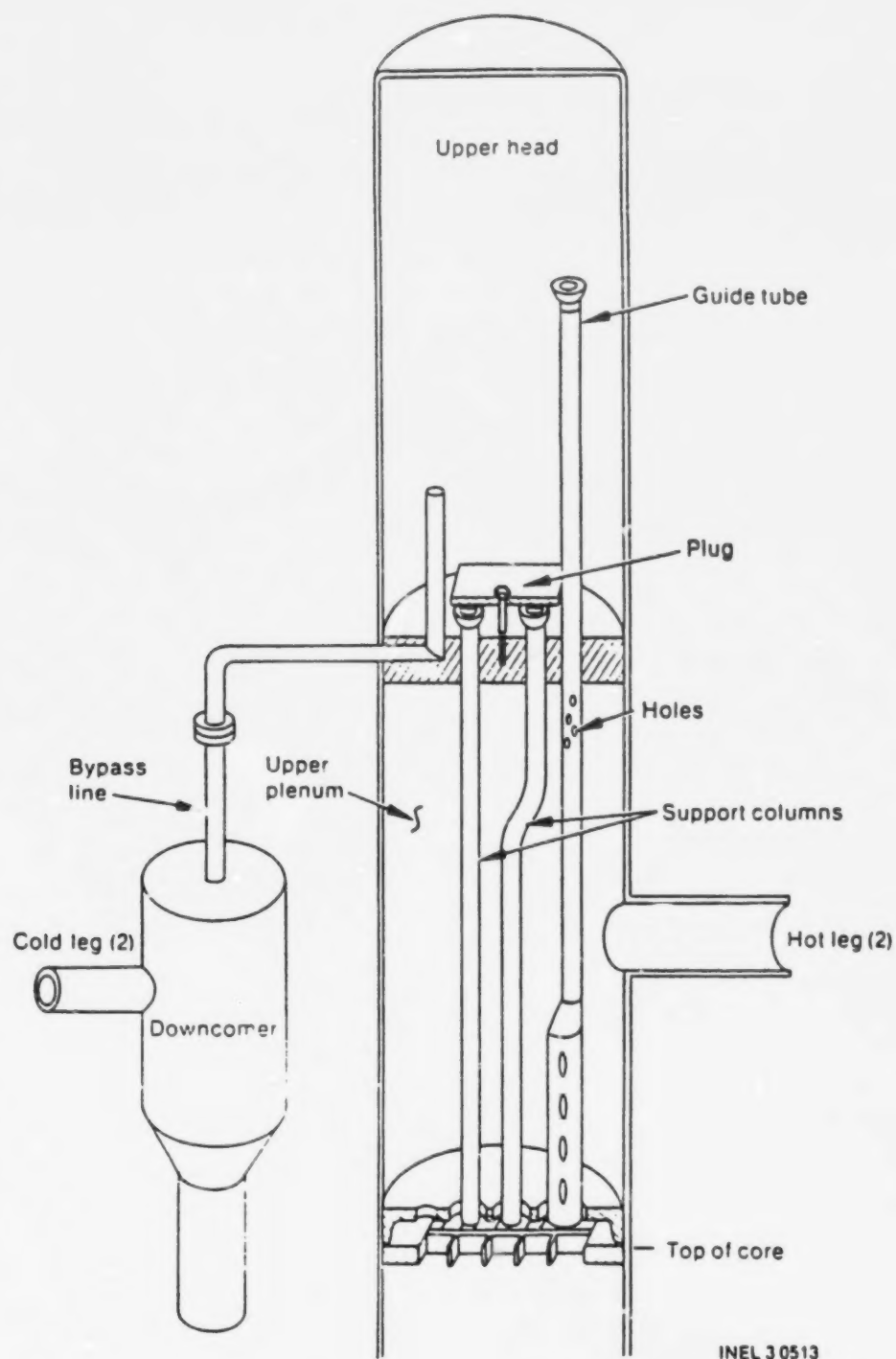


Figure 2. Semiscale Mod-2C vessel upper head configuration.

flow rates were measured with turbine meters. Condensing systems and catch tanks were used to measure effluent from the steam generator atmospheric dump valves and the break assembly (break volumetric flow rate).

EXPERIMENTAL PROCEDURE

As a general procedure before initiation of the transient, the system was filled with demineralized water and vented to ensure a liquid-full system. The system was heated to initial conditions using core power and pumped flow and pressurized using the pressurizer internal heaters to draw a steam bubble. The steam generator secondaries dissipated the core heat to atmosphere by steaming. The Semiscale initial pressures and temperatures were typical of commercial PWR full-power operating hydraulic conditions in the primary and secondary systems.

The transients were initiated by opening a block valve, allowing primary fluid to exit through the break orifice and into the break condensing system. This caused the primary system to depressurize and generate signals that triggered automatic responses by various systems. The reactor scram signal was generated by a low pressurizer pressure of 13.1 MPa (1888 psig), which closed the main steam valves and the crossover line valve and initiated core power decay. The core power scram to the ANS decay curve was delayed 3 s to simulate control rod drop time. The safety injection signal (SIS) was generated by a low pressurizer pressure of 12.51 MPa (1802 psig), which started auxiliary feedwater and stopped main feedwater. The operators also tripped the primary coolant pumps at SIS after a 2-s delay to simulate transformer decay time. The HPI system was assumed to be inoperative throughout the transient, and the auxiliary feedwater flow was assumed to be partially degraded. The auxiliary feedwater system in Zion consists of two motor-driven pumps and one steam turbine-driven pump. It was assumed that the auxiliary feedwater system was degraded so that only one motor-driven pump was available. The correct scaled auxiliary mass flow rate was delivered to the broken loop steam generator, but the maximum possible auxiliary feedwater flow rate to the intact loop steam generator was limited to half the scaled flow rate by pump size. The transients were terminated when the pressurizer pressure reached 1.38 MPa (188 psig), the LPIS pressure setpoint.

The various recovery scenarios then determined what operator actions would be taken. Experiments S-NH-1 and S-NH-2 differed only in break size and initiated steam generator secondary steam-and-feed recovery operations when the PCT reached 811 K (1000°F). Experiment S-NH-3 had the same break size as S-NH-1 but restarted the intact loop pump when the PCT reached 811 K (1000°F) and began steam generator secondary steam-and-feed operations when the PCT reached 950 K (1250°F). Experiment S-NH-5 also had the same break size as S-NH-1 but initiated steam generator steam-and-feed recovery operations when the vessel liquid level declined to the elevation of the top of the core, before a heater rod temperature excursion began. The only other automatic action was accumulator injection when the primary pressure reached 4.24 MPa (600 psig). The operators, depending on the experiment, also closed the steam generator atmospheric dump valves (ADV) upon a low level signal [200 cm (78.7 in.)] from the steam generator secondary and reopened them when the level rose to 400 cm (157.4 in.).

EXPERIMENTAL RESULTS

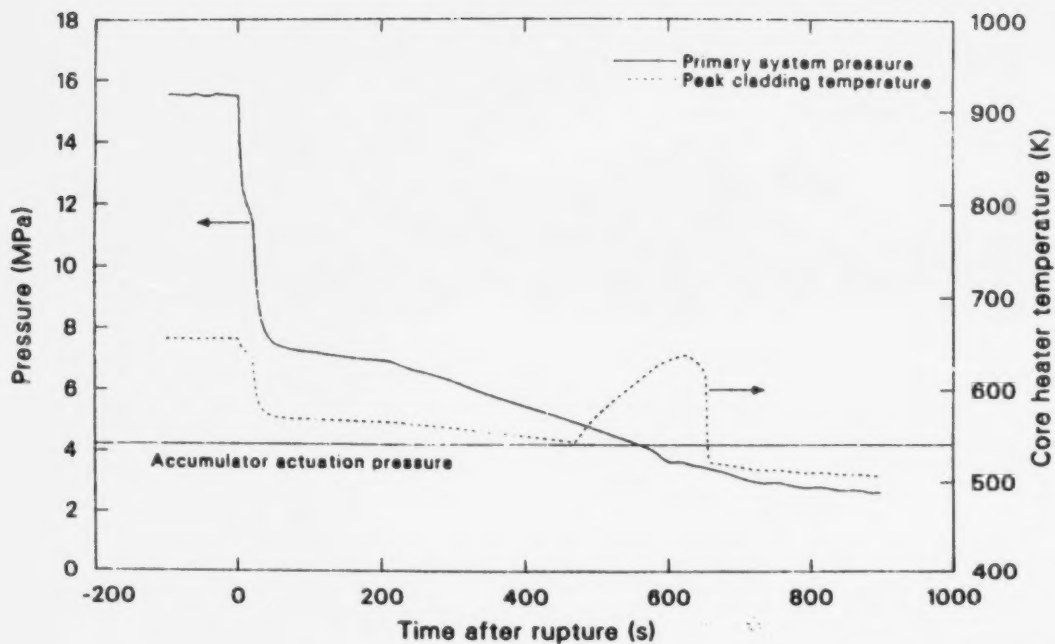
The shear of an instrumentation line or the cracking of a primary coolant pipe in a PWR results in significant loss of primary system inventory and a rapid depressurization of the primary coolant system. The larger the break the faster the primary system depressurization and primary system inventory depletion. For a 5% break, simulated in Experiment S-LH-2,¹² the primary system depressurized to the accumulator actuation pressure before a significant core heater rod temperature excursion occurred (Figure 3). However, for a 0.5% break (Experiment S-NH-1) and a 2.1% break (Experiment S-NH-2) without high pressure injection (HPI), the primary system did not depressurize to the accumulator actuation pressure before inadequate core cooling conditions, resulting in a core heater rod temperature excursion (Figures 4 and 5). Operator action was necessary to reduce the primary system pressure to the accumulator actuation pressure in these two experiments.

The HPI system was assumed inoperative for all the experiments in the S-NH test series, but was in operation during Experiment S-LH-2. The HPI mass flow was not significant in Experiment S-LH-2 and averaged only 3% of the break mass flow rate prior to accumulator injection. With the smaller breaks in Experiments S-NH-1 and S-NH-2, the HPI mass flow (if available) would have been a much larger portion of the break flow and may have equaled the break flow in Experiment S-NH-1. This would have resulted in a slower primary system inventory depletion and a faster primary system depressurization (as a result of injecting cold water). If the primary system inventory had been increased by HPI mass flow, the core heater rod temperature excursion would have been delayed and would have been less severe, if it occurred at all.

Examining the fluid mass inventory of the primary system alone is misleading in judging transient severity. For instance, core rod heatups occurred for a wide range of overall mass inventories starting at about 35%. However, once accumulator injection was initiated, the core remained cool with a mass inventory as low as 18%. The fluid mass distribution in the system (particularly the vessel), rather than the overall mass inventory, determined the core thermal response to the accident.

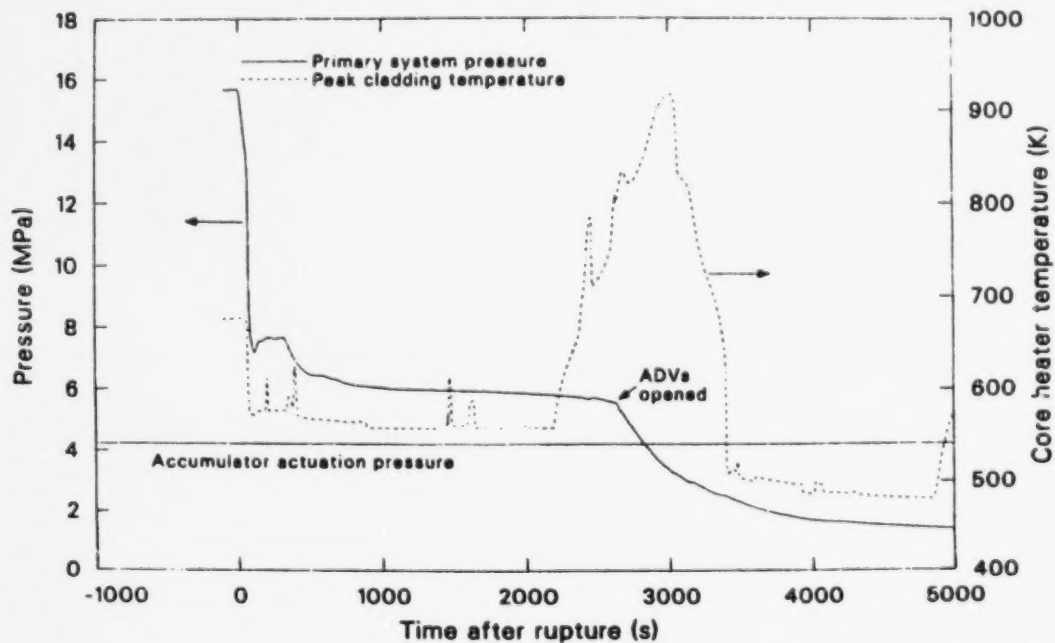
It is the froth level (Figure 6) rather than the collapsed liquid level that gives a more physical representation of the actual hydraulic conditions in the core. The froth level, which extends above the collapsed liquid level, is the upper limit of a two-phase mixture, above which is steam and below which is a single-phase liquid pool or two-phase mixture. With a film of liquid on the rod wall, nucleate boiling would remove the decay heat and preclude a rod heatup. The froth level, then, must drop below a point on the heater rod for a heater rod dryout and temperature excursion to occur at that point. However, the froth level is difficult to measure; it is determined by using the elevation of the densitometer and the time the density shows a sudden drop from two-phase to steam conditions. In the Semiscale facility, there are only six densitometers through the core region to determine the froth level. The froth level also is not uniform; it is lower at

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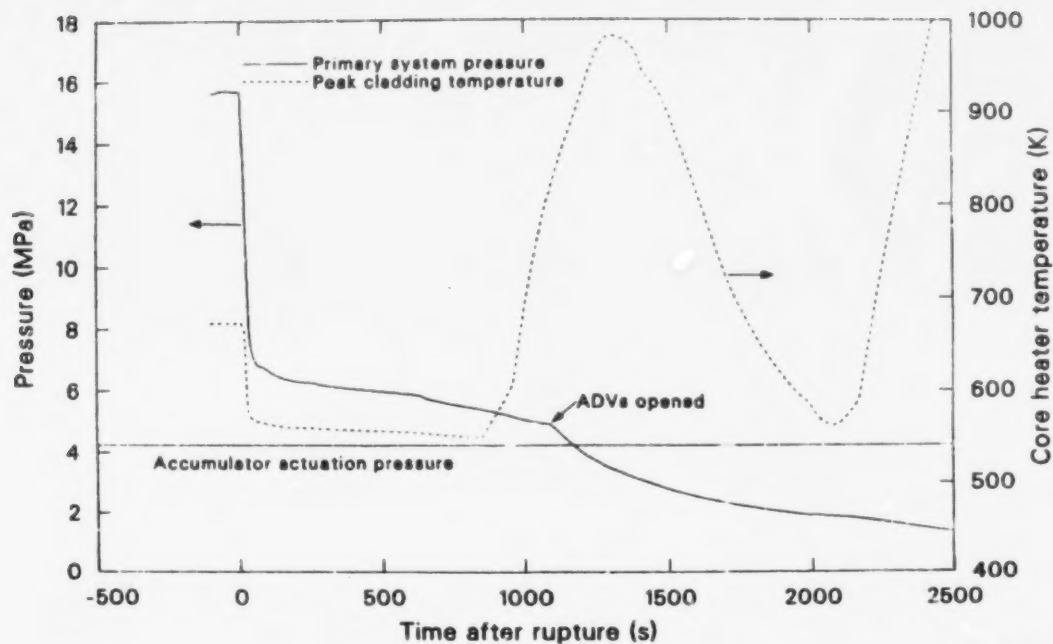
Figure 3. Peak heater rod cladding temperature and primary system pressure during 5.0% SBLOCA Experiment S-LH-2.



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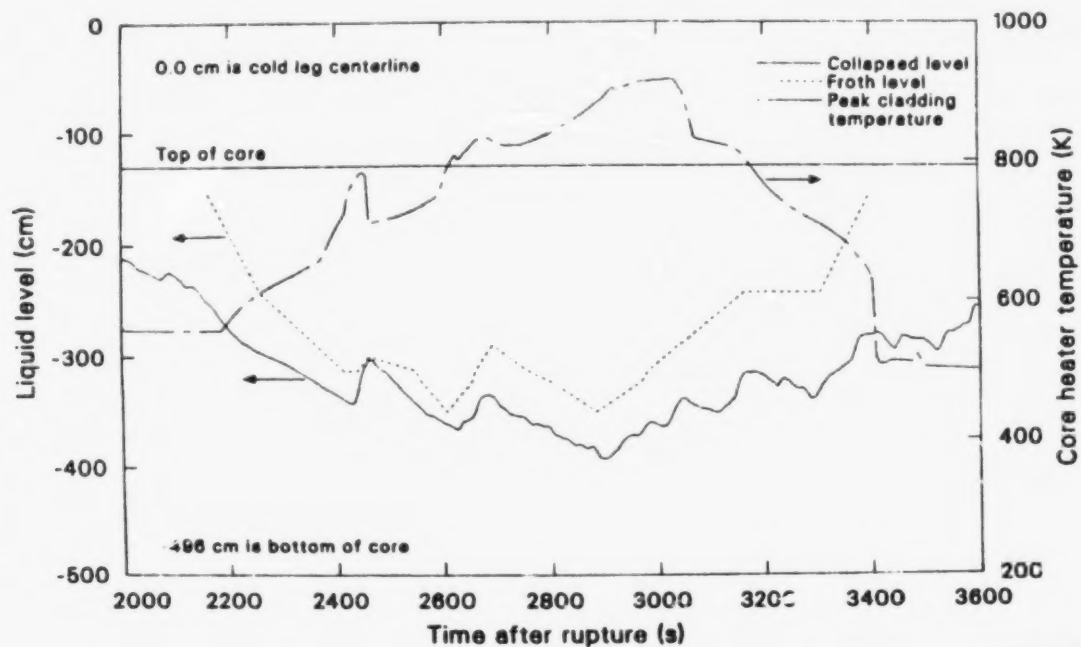
Figure 4. Peak heater rod cladding temperature and primary system pressure during 0.5% SBLOCA Experiment S-NH-1.

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Figure 5. Peak heater rod cladding temperature and primary system pressure during 2.1% SBLOCA Experiment S-NH-2.



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Figure 6. Peak heater rod cladding temperature, froth level, and vessel collapsed liquid level during 0.5% SBLOCA Experiment S-NH-1.

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the core walls and higher in the core center. However, basically the core heater rod heatups occurred in a uniform top-down manner following the froth level.

Influence of Break Size on Transient Severity

The fluid mass inventory of the primary system (Figures 7 and 8) when the temperature excursion began was 35% for Experiment S-NH-1 and 25% for Experiment S-NH-2. However, the vessel collapsed liquid levels and the froth levels (Figures 6 and 9) were about the same when the core temperature excursions began. The vessel in the two experiments, then, contained about the same amount of fluid when the temperature excursions began, although the total primary system fluid mass inventory was lower in Experiment S-NH-2. The rate the core heated up depended solely on the froth level. As the froth level dropped, heater rod dryouts occurred and the PCT began to rise. The PCT rose slowly at first as dryouts occurred in the top of the core where the power density was lowest. As the froth level dropped to the middle of the core, the PCT increased at a faster rate. The rate at which the PCT rose at a given froth level was the same for both Experiments S-NH-1 and S-NH-2. The larger temperature excursion in Experiment S-NH-2 was the result of the larger mass inventory depletion of the vessel caused by the larger break. In addition, in Experiment S-NH-1 the broken loop steam generator U-tubes drained after the temperature excursion began, partially reflooded the core, and quenched the heater rods in the lower and middle portions of the core. This redistribution of primary system mass delayed the core heatup and did not happen in Experiment S-NH-2.

Response to Recovery Operations. The recovery operations for experiments S-NH-1 and S-NH-2 consisted of secondary steam-and-feed operations by latching open both steam generator secondary atmospheric dump valves (ADVs) when the PCT reached 811 K (1000°F) and normal accumulator injection. Auxiliary feedwater was used to maintain the secondary levels. Opening the steam generator ADVs allowed steam, with its energy, to exit the steam generators. As a result, both secondary pressures and temperatures dropped and created heat sinks for the primary system (Figure 10). This resulted in the condensation of steam and caused a rapid drop in the primary system pressure to the accumulator actuation pressure of 4.22 MPa (600 psig), allowing water to flow out of the accumulators. This water flowed through the cold legs and immediately began to fill the downcomer and vessel (Figures 6 and 9). The PCT, however, continued to increase at the same rate until the froth level in the vessel rose to the level where the PCT was the highest. The accumulator operation quickly stopped the rise of the PCT as temperature reached a maximum of 917 K (1191°F) and 983 K (1304°F) for Experiments S-NH-1 and S-NH-2, respectively. As the vessel continued to fill, the steam generation rate in the core increased which increased the froth level and cooled the heater rods. In Experiment S-NH-1, the core was completely quenched and returned to the saturation temperature. However, in Experiment S-NH-2, the accumulators emptied before the core returned to the saturation temperature and a second core heatup began.

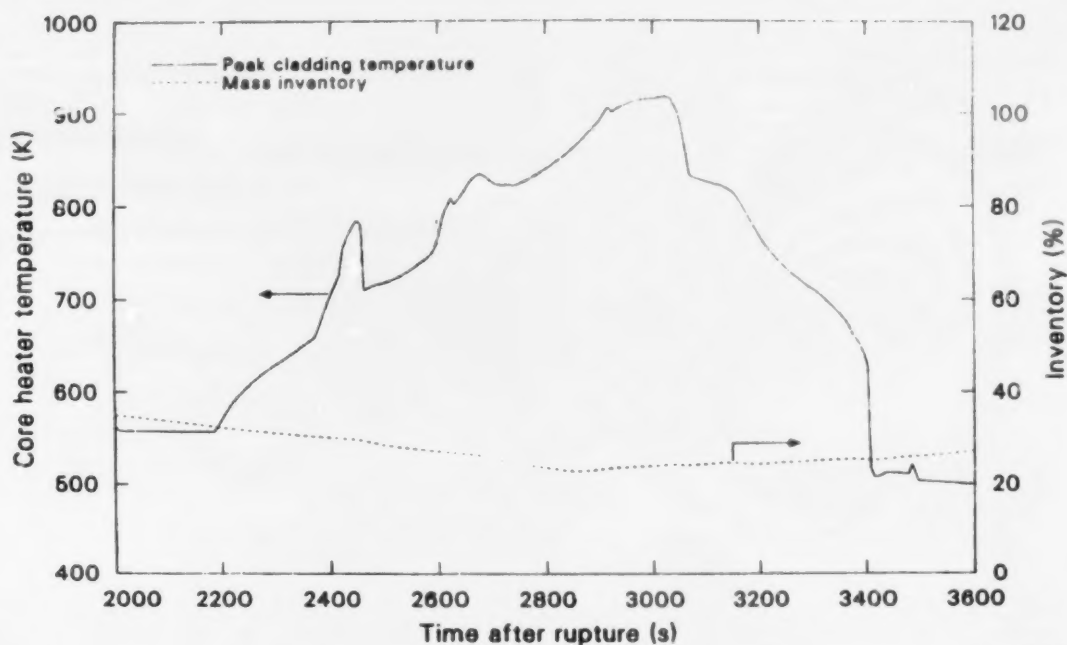


Figure 7. Peak heater rod cladding temperature and normalized primary mass inventory during 0.5% SBLOCA Experiment S-NH-1.

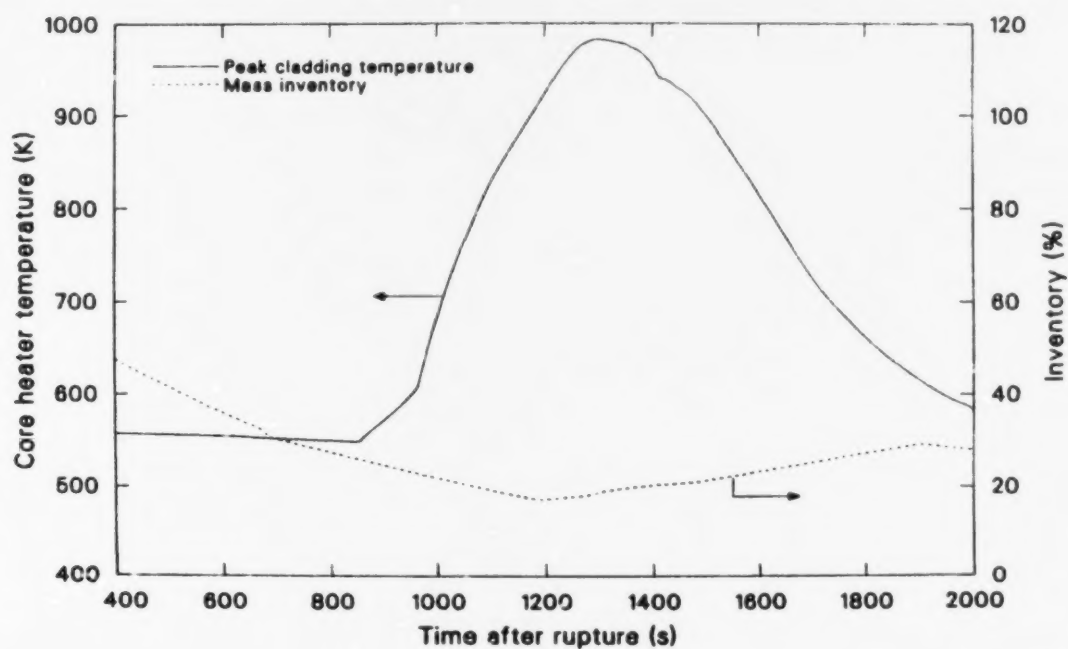


Figure 8. Peak heater rod cladding temperature and normalized primary mass inventory during 2.1% SBLOCA Experiment S-NH-2.

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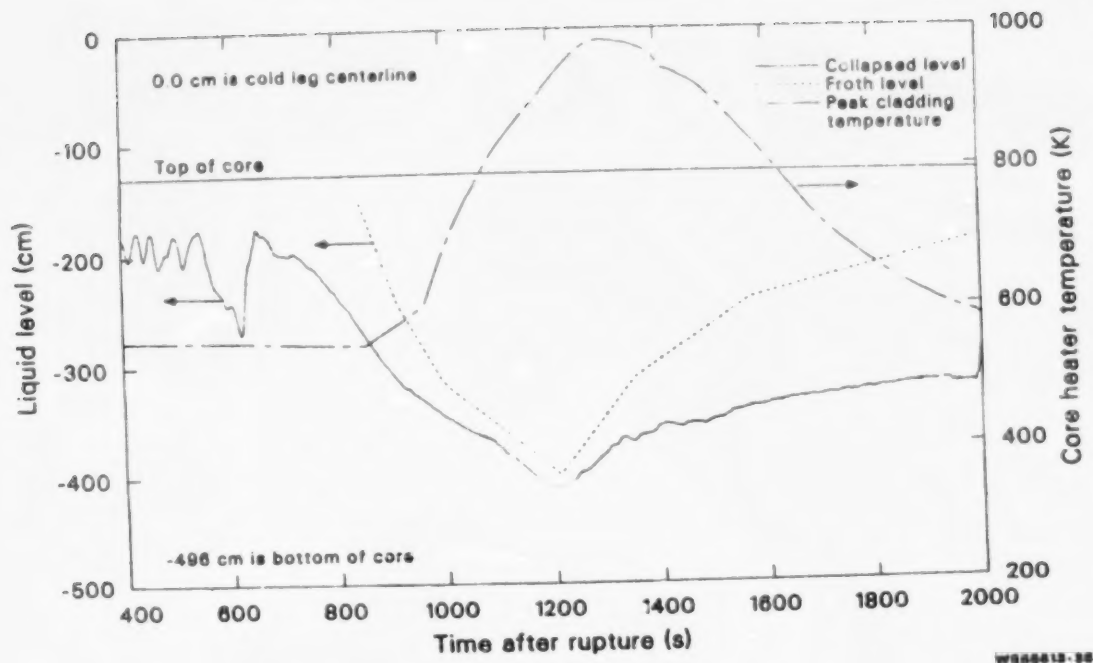


Figure 9. Peak heater rod cladding temperature, froth level, and vessel collapsed liquid level during 2.1% SBLOCA Experiment S-NH-2.

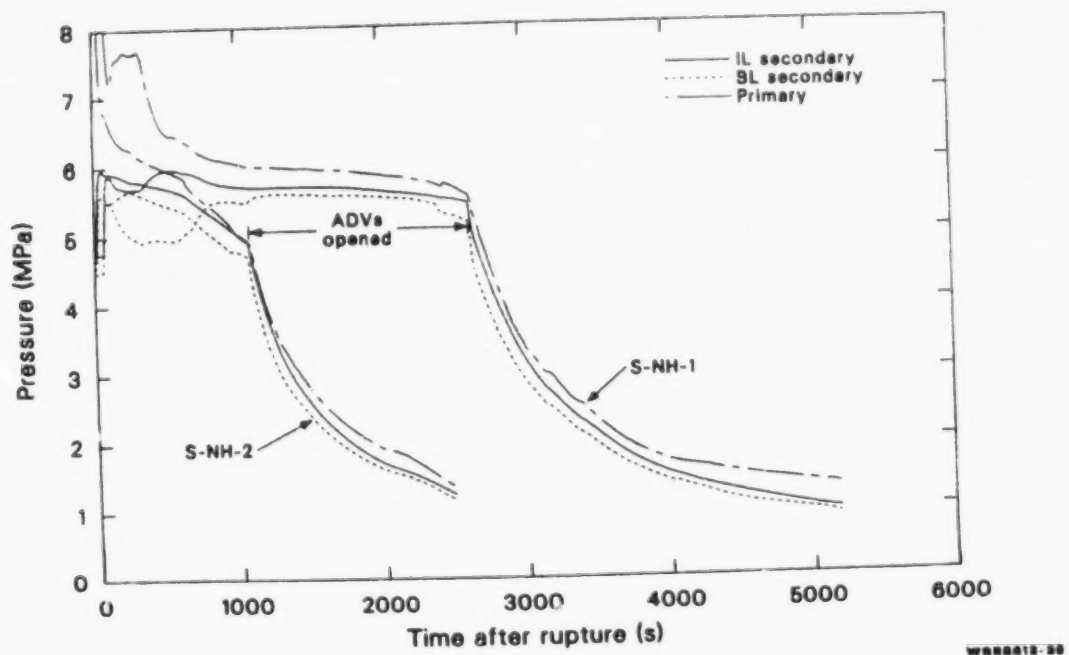


Figure 10. Comparison of primary and secondary system pressures during SBLOCA Experiments S-NH-1 and S-NH-2.

In both Experiments S-NH-1 and S-NH-2, the accumulators emptied before the LPIS pressure setpoint (experiment termination) was reached and a second core boiloff began. In Experiment S-NH-2, this resulted in a second core thermal excursion. However, in Experiment S-NH-1, the continued ADV operation reduced the primary system pressure to the LPIS pressure setpoint before a core heatup occurred.^a This second core boiloff was a basic top-down boiloff with water in the vessel being boiled off by the heater rods and then condensed in the steam generator U-tubes. The continued ADV operation also reduced the primary system pressure to the LPIS pressure setpoint in Experiment S-NH-2, but not before the PCT reached 1046 K (1423°F).

Influence of Recovery Operations on Transient Severity

Experiments S-NH-1, S-NH-3, and S-NH-5 all had the same break size (0.5%) and differed only in the recovery procedures. In Experiment S-NH-1, steam generator secondary steam-and-feed recovery operations were initiated when the PCT reached 811 K (1000°F). In Experiment S-NH-3, the intact loop primary coolant pump was restarted when the PCT reached 811 K (1000°F); and the steam generator secondary steam-and-feed recovery operations were initiated when the PCT reached 950 K (1250°F). In Experiment S-NH-5, the steam generator secondary steam-and-feed operations were initiated when the vessel liquid level reached the elevation of the top of the core, before a heater rod temperature excursion began.

Pump Restart. When the intact loop pump was restarted in Experiment S-NH-3, liquid was in the intact loop pump suction forming a seal that blocked fluid flow. Restarting the pump pulled this liquid out of the pump suction and forced it, along with the liquid in the downcomer, into the vessel. As a result, the core liquid level rose; and the heater rods were immediately quenched (Figure 11). The increased core liquid level also increased the vapor generation rate in the core, pressurizing the primary system. This increased the break flow rate, causing the primary system inventory to be depleted at a faster rate.

After forced flow was established at 2850 s, a 650-s period of quasi-steady state followed. The primary system pressure remained constant, while the intact loop steam generator secondary pressure rose slightly in response to the enhanced primary-to-secondary heat transfer caused by the forced convection as a result of the pump operation. The broken loop steam generator secondary pressure fell and rose slightly in response to the changes in the broken loop U-tube liquid level. The loop fluid temperatures during this period were also essentially constant, remaining near their respective steam generator saturation temperatures. The primary system, however, continued to lose mass through the break. Nearly all the inventory lost through the break came from the vessel and downcomer as the fluid

a. After test termination conditions were attained in Experiment S-NH-1, 200 s of additional data were recorded to find out at what vessel liquid level the second core heatup would occur.

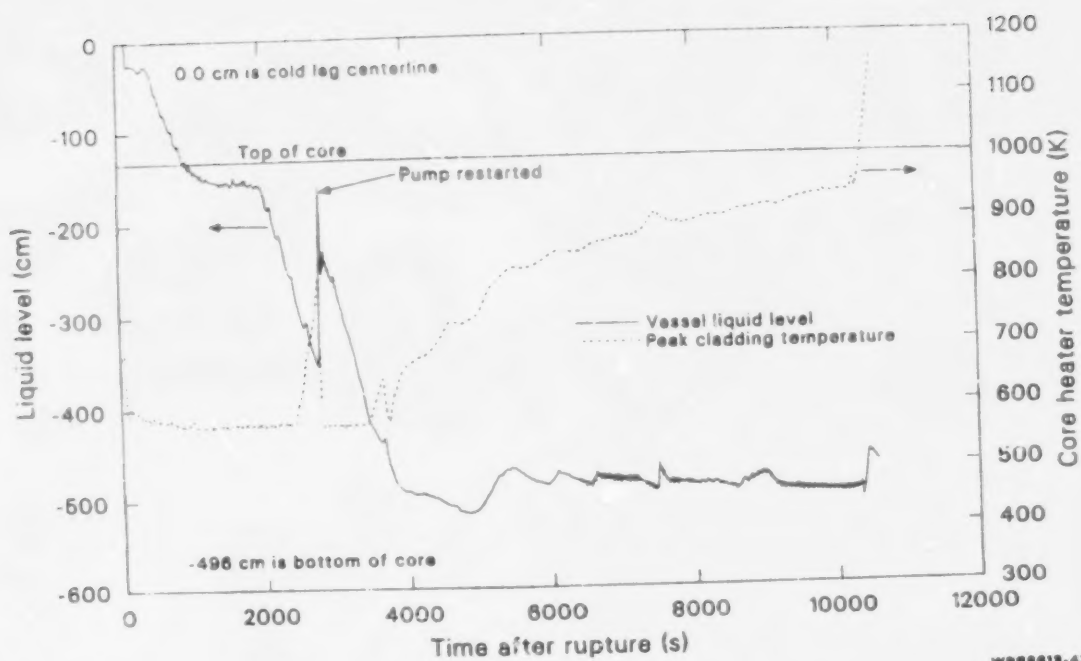


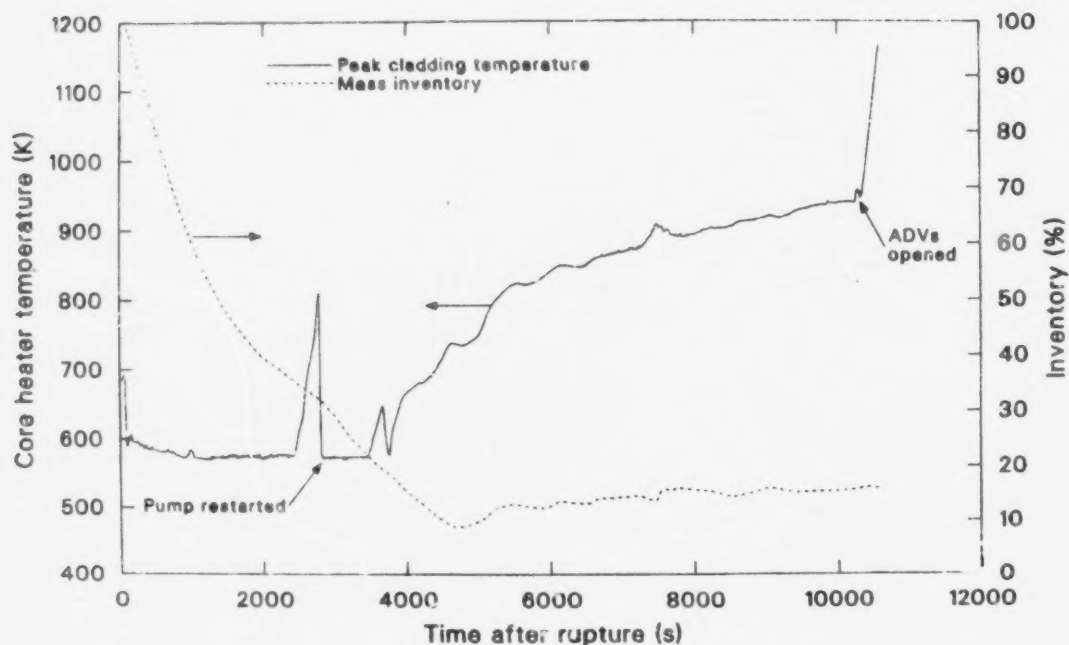
Figure 11. Peak heater rod cladding temperature and vessel collapsed liquid level during 0.5% SBLOCA with pump restart Experiment S-NH-3.

inventory and distribution in the other sections of the primary system remained unchanged. By 3500 s, the primary system inventory had fallen to 21%; and the core liquid level had decreased sufficiently that core cooling was no longer adequate and the core began to heat up (Figure 12).

This heatup, however, was slower than the first heatup and was also slower than the heatup observed in Experiment S-NH-1, as a result of the continued intact loop pump operation. It took 4630 s longer for the PCT to rise to 916 K (1189°F), the maximum PCT in Experiment S-NH-1, in Experiment S-NH-3 than in Experiment S-NH-1 (Figure 13). Shortly after the heatup began, the broken loop U-tubes drained and momentarily quenched the core similar to the phenomenon observed in Experiment S-NH-1. This was due to the reduction in vapor generation in the core as a result of the core heatup. The vapor generation in the core is reduced as the core dries out and heats up as less and less of the core is covered with liquid. As the vapor generation in the core is reduced, less vapor flows into the broken loop piping and the flooding condition can no longer be supported in the U-tubes. The fluid in the up-side of the U-tubes drained back into the vessel as the broken loop hot leg density showed a marked increase at this time.

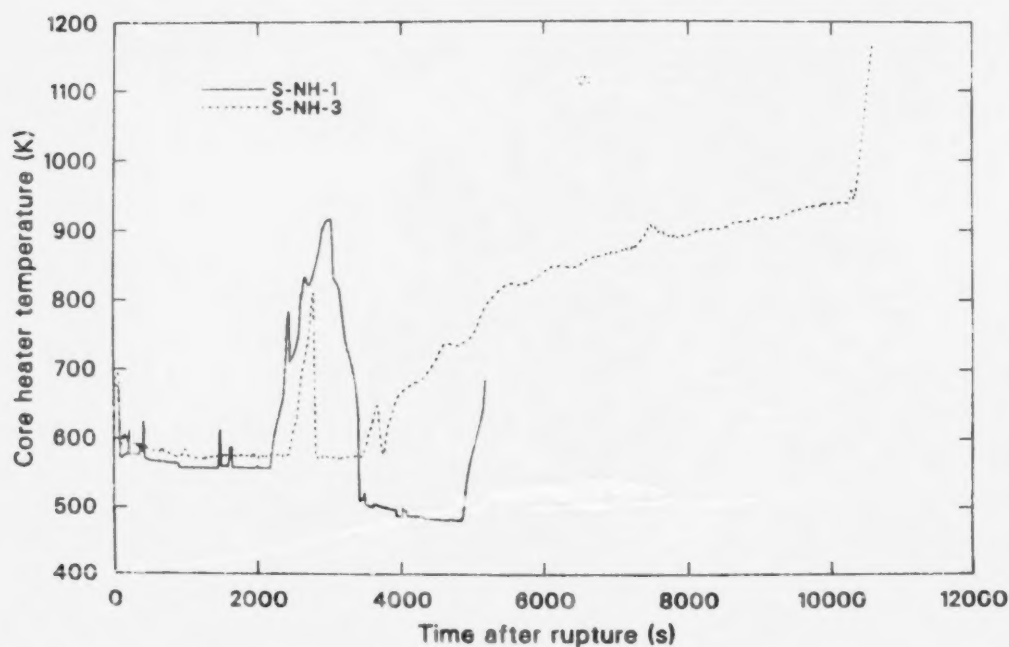
As the core liquid level dropped and the heater rods dried out, the vapor generation rate in the core decreased. The primary system pressure, as a result, dropped and reached the accumulator actuation pressure at 4575 s. There were, however, no drastic changes in response to the

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Figure 12. Peak heater rod cladding temperature and normalized primary mass inventory during 0.5% SBLOCA with pump restart Experiment S-NH-3.



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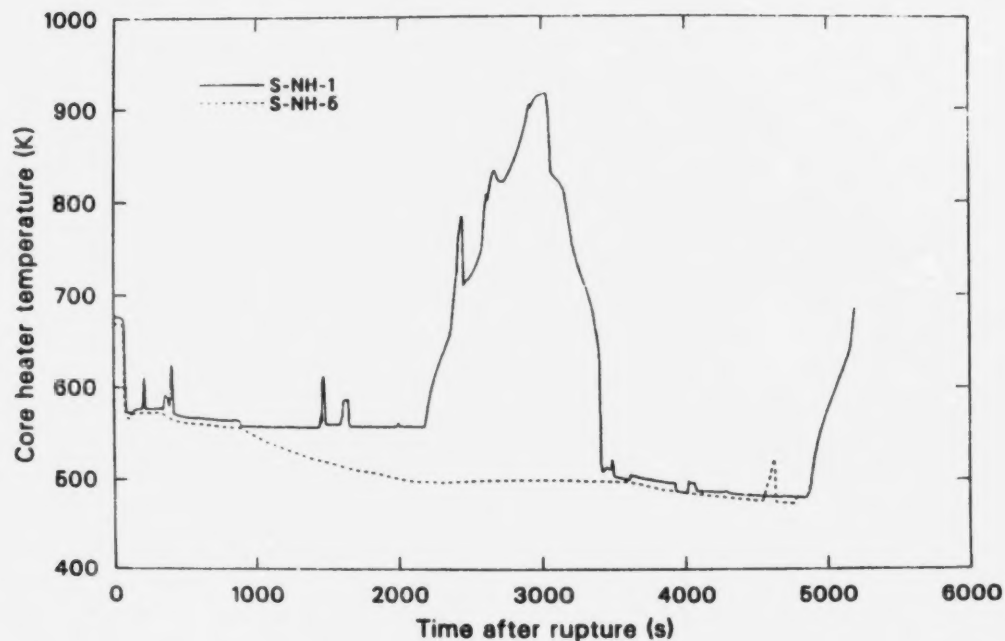
Figure 13. Comparison of peak heater rod cladding temperatures during 0.5% SBLOCA Experiments S-NH-1 and S-NH-3.

accumulator injection. The PCT continued to gradually increase, although slight oscillations were superimposed on the general behavior because of accumulator injection cycling. The rate the PCT rose did, however, decrease with time. This decrease was due to the increasing temperature difference between the core inlet fluid and the heater rods due to accumulator injection. The primary system pressure continued to decrease, but at a decreased rate as the water from the accumulators was heated and vaporized in the core. The break flow decreased with the primary system depressurization and was almost equal to the accumulator injection flow as the primary system inventory remained about 15%.

The PCT continued to gradually increase and reached 950 K (1250°F) at 10280 s when secondary steam-and-feed operations commenced by opening both steam generator ADVs. As a result, the steam generator secondary pressures immediately dropped. The intact loop secondary pressure, which had been increasing as a result of the superheated steam being pumped through the intact loop piping, dropped much more than the broken loop secondary pressure. The intact loop secondary pressure was 5.42 MPa (786 psi) greater than the primary system pressure, while the broken loop secondary pressure was 0.1 MPa (14.5 psi) lower than the primary system pressure when the ADVs were opened. The primary system pressure also decreased, generally following the broken loop secondary pressure, but the PCT showed no immediate change to the slight temperature rise it was experiencing. The intact loop pump was inadvertently tripped off at 10330 s so the effect on the primary system due to the opening of the steam generator ADVs cannot be fully studied. The trip was a low pressure safety trip that was not bypassed during the experiment. With the pump off, the PCT rose rapidly as natural circulation replaced the forced convection and increased heat transfer provided by the pump operation.

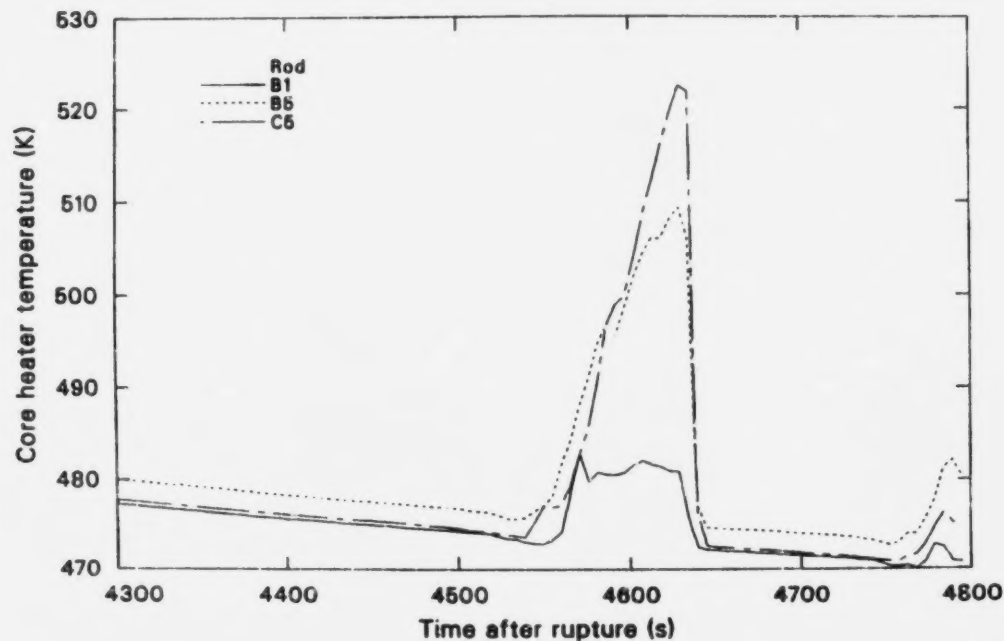
Early Secondary Steam-and-Feed Operations. Latching open the steam generator ADVs when the vessel liquid level reached the top of the core in Experiment S-NH-5 prevented any core thermal excursion from occurring. The ADVs were latched open when the primary system inventory was about 62%; this immediately lowered the secondary pressures which increased the primary-to-secondary heat transfer and, as a result, lowered the primary system pressure. The primary system pressure quickly dropped to the accumulator actuation pressure, allowing water from the accumulators to flow into the primary system and refill the downcomer and vessel. Unlike Experiment S-NH-1, the steam generator U-tubes were liquid-filled when the ADVs were opened, so mass was not drawn out of the core and into the U-tubes as a result of the increased condensation. The vessel level, therefore, did not drop low enough for a heater rod temperature excursion to occur.

Beginning the secondary steam-and-feed recovery operations when the vessel liquid level reached the top of the core, instead of waiting until the PCT reached 811 K (1000°F), prevented the heater rod temperature excursion seen in Experiment S-NH-1 (Figure 14). However, the accumulators emptied before the LPIS pressure setpoint was reached; and several of the heater rods in the upper portions of the core did experience minor heatups (Figure 15). These heatups were quenched when the water in the short U-tube



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Figure 14. Comparison of peak heater rod cladding temperatures during 0.5% SBLOCA Experiments S-NH-1 and S-NH-5.



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Figure 15. Selected heater rod cladding temperatures during 0.5% SBLOCA with early recovery Experiment S-NH-5.

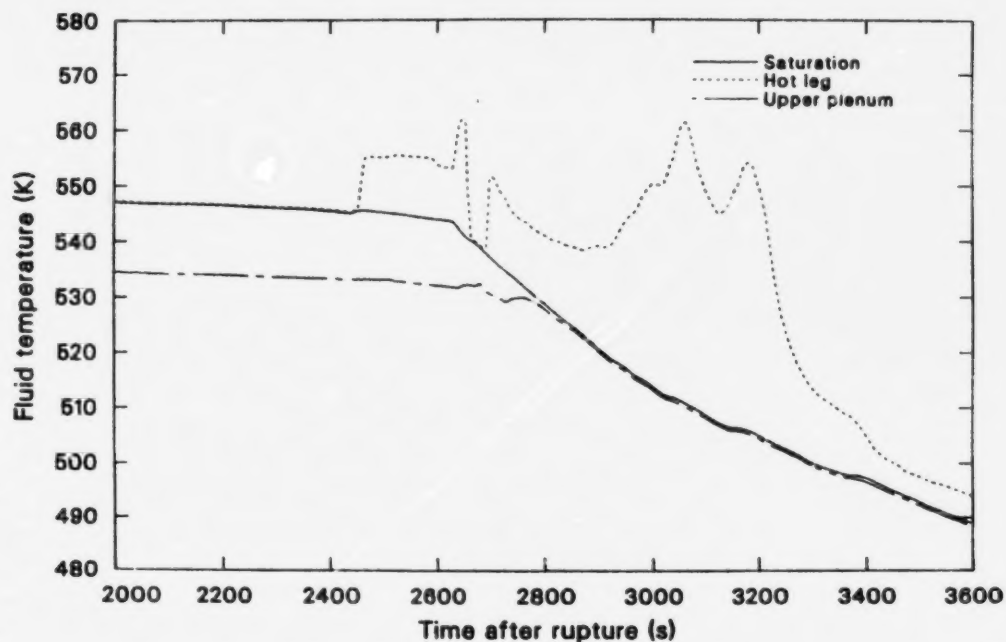
in the broken loop steam generator was forced over the top of the U-tube and partially refilled the core. All of the heatups occurred at the same elevation in the core [320 cm (126 in.) above the bottom of the core] but varied in magnitude. All the thermocouples at this elevation were on heater rods on the outside rows, so it cannot be determined if the walls affected the fluid flow and caused the heatups or if a steam bubble caused all the heater rods to dry out at this elevation. These heatups were, however, very minor, raising the heater rod temperature at most 50 K (27°F) before the LPIS pressure setpoint was reached.

Initiating the secondary steam-and-feed recovery operations early in the transient also resulted in depleting the secondary system mass more in Experiment S-NH-5 than in Experiment S-NH-1. The intact loop steam generator secondary liquid level fell below the low-level setpoint, and the intact loop ADV closed. This did not happen in the broken loop steam generator, as the broken loop ADV flow fell below the auxiliary feedwater mass flow so the broken loop steam generator secondary liquid level was maintained. However, the intact loop auxiliary feedwater mass flow was only half the scaled flow, although the auxiliary feedwater flow was assumed to be degraded.^a If the scaled intact loop auxiliary feedwater mass flow had been available, the intact loop low-level setpoint would not have been reached and the intact loop ADV would not have closed. The LPIS pressure setpoint would have been reached earlier, but the accumulators would still have emptied prior to experiment termination.

RECOVERY INITIATION DETERMINATION

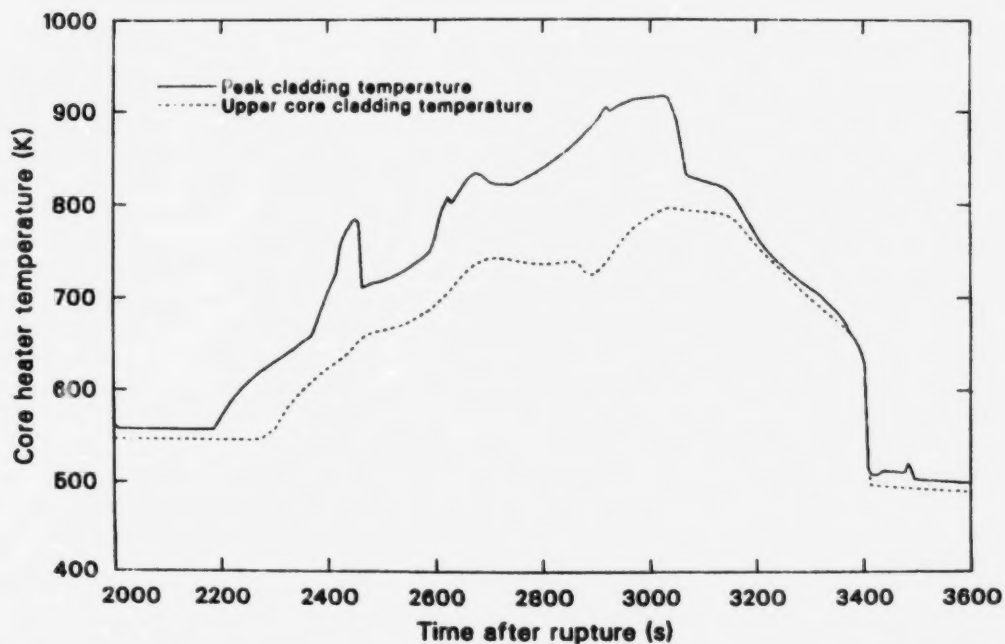
Recovery operations in the S-NH test series were initiated when the PCT reached 811 K (1000°F), not when the core exit thermocouples reached 922 K (1200°F) as set forth in the emergency operating procedures (EOP-11) for the Zion nuclear plant. This was done for safety reasons and did not affect the phenomena of interest. In the Semiscale facility, there is only one thermocouple near the exit of the core and that is in the upper plenum. This thermocouple (Figure 16) showed no superheat and measured basically only the fluid temperature which was either subcooled or saturated. This thermocouple, however, is not at the exit of the core and is influenced by fluid flowing from the hot legs and upper head. The steam in the hot legs, however, did become as much as 55 K (99°F) superheated. This is still well below the peak cladding temperature. It has been reported¹³ that the core exit vapor temperatures tend to follow the cladding temperatures in the upper regions of the core. In Experiment S-NH-1, these temperatures were about 110 K (200°F) cooler than the PCT (Figure 17) when the recovery procedures were initiated. This temperature difference was increasing and would have been about 250 K (450°F) had the recovery operations not begun

a. The auxiliary feedwater system in Zion consists of two motor-driven pumps and one steam turbine-driven pump. It was assumed that the auxiliary feedwater system was degraded so that only one motor-driven pump was available. This flow rate was still twice the maximum flow rate available in Semiscale.



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Figure 16. Saturation and fluid temperatures in vessel upper plenum and hot legs during 0.5% SBLOCA Experiment S-NH-1.



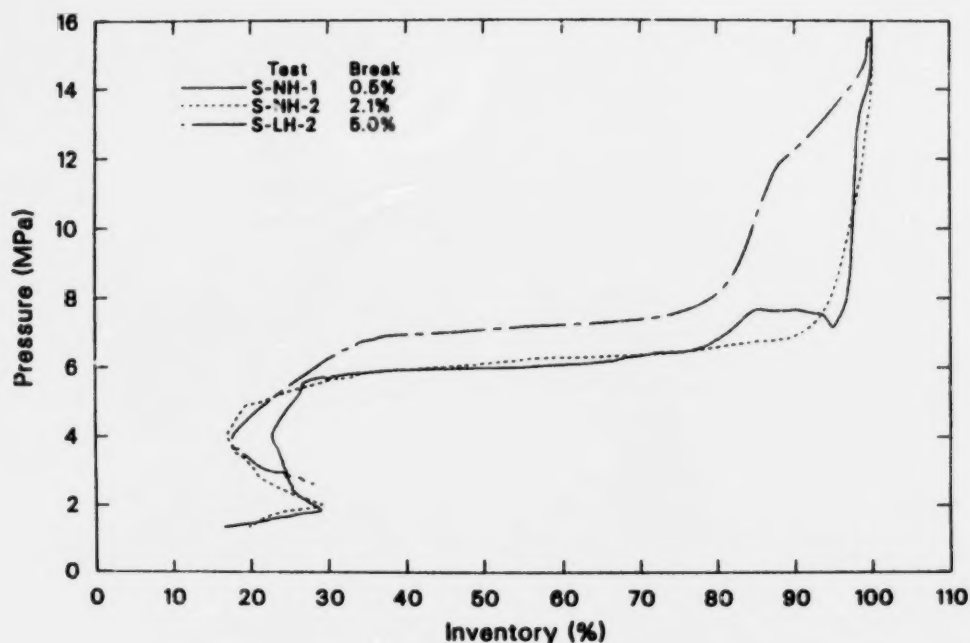
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Figure 17. Peak heater rod cladding temperature and upper core cladding temperature during 0.5% SBLOCA Experiment S-NH-1.

until the cladding temperature in the upper regions of the core reached 811 K (1000°F). Therefore, if recovery operations were not started until inadequate core cooling was signaled by the cladding temperature in the upper regions of the core reaching 922 K (1200°F), the cladding temperatures in the center of the core would have been at least 1175 K (1650°F). The PCT continued to rise an additional 110 K (200°F) after the initiation of recovery operations and would have reached at least 1285 K (1850°F) before decreasing. This calculation of the maximum PCT was very conservative, but indicates that fuel damage could have resulted.

To prevent fuel damage, a better basis for operator initiation of recovery operations is needed. The problem is that in a SBLOCA without HPI the energy balance is such that the primary system pressure remains above the accumulator actuation pressure and inadequate core cooling results. There are two ways to remove energy from the primary system and reduce the primary system pressure to the accumulator actuation pressure faster. One is to open a pressurizer power operated relief valve (PORV), which essentially increases the break size. The other is to open the steam generator ADVs and initiate secondary steam-and-feed operations. Opening a PORV releases mass from the primary coolant system. This is undesirable. However, this mass should be in the form of steam so that the primary system energy removal should be relatively large for the amount of mass lost. The primary system pressure decrease should be significant, and the additional primary mass loss should be minimal. However, the primary system inventory will also be redistributed and may cause unforeseen problems (such as drawing liquid from the core and filling up the pressurizer). Experiments S-NH-1 and S-NH-2 showed that secondary steam-and-feed operations could reduce the primary system pressure to the accumulator actuation pressure after inadequate core cooling conditions were reached. Experiment S-NH-5 showed that if steam-and-feed recovery operations are started earlier no heater rod temperature excursions will occur.

Since secondary steam-and-feed operations can remove sufficient energy from the primary system to reduce the primary system pressure to the accumulator actuation pressure with no additional loss of primary coolant, secondary steam-and-feed operations are a better way of reducing the primary system pressure than PORV operation. The question is, however, when is the best time to start the recovery operations. System response to a SBLOCA is characterized as a primary depressurization with significant loss of primary system inventory (Figure 18). With no HPI, the primary system mass inventory continues to decrease until accumulator flow is initiated. Initially, the primary system pressure drops rapidly, but slows and becomes very stable after reaching saturation conditions. In a SBLOCA, the primary and secondary systems remain coupled so the primary system pressure cannot drop below the average secondary system pressure. If, however, the break flow becomes single-phase steam, the energy lost out the break becomes larger than the energy input by the core, and the primary and secondary systems decouple. This allows the primary system pressure to drop below the average secondary pressure. This happened early in the 5.0% break Experiment S-LH-2, so the primary system pressure fell to the accumulator actuation pressure without the aid of operator actions. The break flow also became single-phase steam

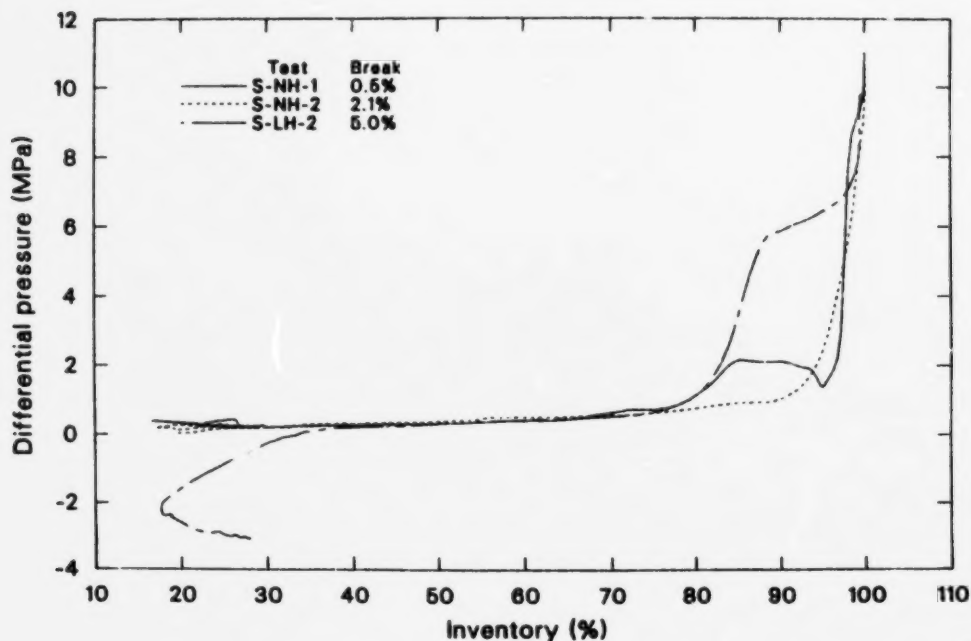


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Figure 18. Primary system pressures as a function of the normalized primary mass inventory during SBLOCA Experiments S-NH-1, S-NH-2, and S-LH-2.

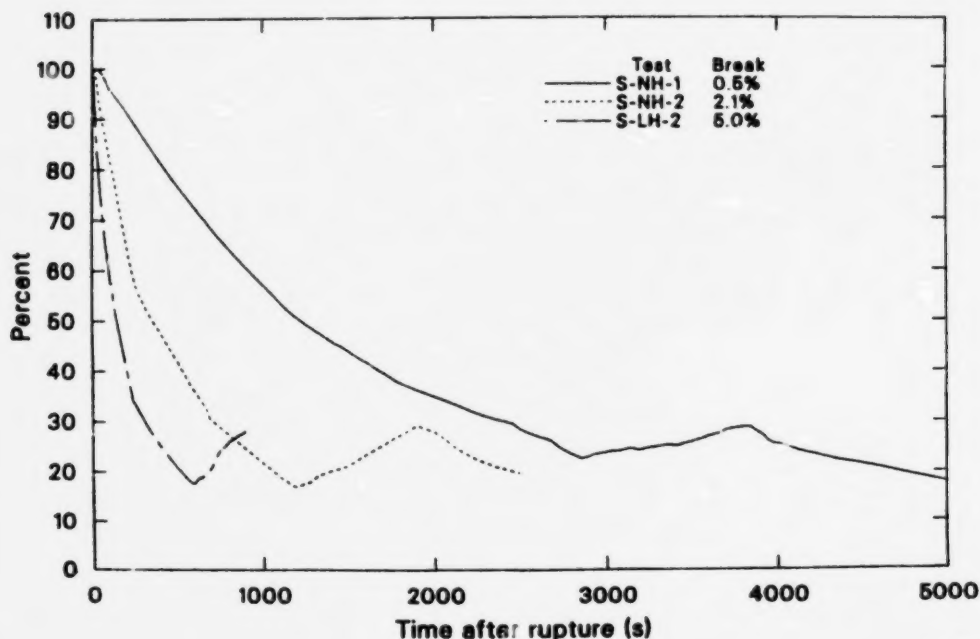
in the 2.1% break Experiment S-NH-2, but not until inadequate core cooling conditions existed. As shown in Figure 18, regardless of break size, the primary system pressure drops rapidly until the primary system inventory reaches 75%. As the primary system inventory decreases from 75% to 35%, there is little decrease in primary system pressure. The primary system pressure approaches the secondary pressures in the isolated steam generators. It is during this time, then, that steam-and-feed operations should begin. In Experiment S-NH-5, the primary system inventory was 62% when secondary steam-and-feed operations were initiated. At the very least, secondary steam-and-feed operations should begin when the primary system pressure falls below the secondary steam generator pressures and primary-to-secondary heat transfer is lost. (This happens at a primary system mass inventory of about 35% in the larger breaks.) The secondaries then become energy sources rather than energy sinks for the primary system. To ensure adequate primary system energy removal and depressurization, secondary steam-and-feed operations should begin when the primary system pressure nears the secondary steam generator average pressure. Figure 19 shows that, regardless of break size, the average primary-to-secondary system pressure differential as the primary system inventory decreased from 75% to 35% was about 0.5 MPa (72.5 psi).

There is also a question of how much time an operator has to initiate secondary steam-and-feed operations under such conditions. The larger the break, the less the time (Figure 20). For a 5.0% break, it takes about 65 s



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Figure 19. Average primary-to-secondary pressure differential as a function of the normalized primary mass inventory during SBLOCA Experiments S-NH-1, S-NH-2, and S-LH-2.



WNR8013-01

Figure 20. Comparison of normalized primary mass inventories during SBLOCA Experiments S-NH-1, S-NH-2, and S-LH-2.

for the primary system inventory to drop to 70% and 230 s to drop to 35%; for a 2.1% break, about 170 s and 620 s; and for a 0.5% break, about 660 s and 2000 s. However, in the larger breaks (5.0% and larger), the primary system pressure declines to the accumulator actuation pressure before inadequate core cooling occurs (although a core heatup does occur). In the smaller breaks, the primary system pressure does not decrease as fast; and operator action is necessary to lower the primary system pressure to the accumulator actuation pressure. Once the accumulator actuation pressure has been reached, water from the accumulators can quench the core. If after the accumulators have emptied the primary system pressure is not sufficiently close to the LPIS actuation pressure, then an additional core heatup may result; and additional operator action such as opening a PORV may be necessary.

CONCLUSIONS

The following conclusions have been drawn based on the analyses of the experimental data from the S-NH test series.

1. Inadequate core cooling conditions can result in small breaks without HPI flow unless the operator takes appropriate action. Although the primary system pressure response for all SBLOCAs has the same characteristic inflection points regardless of break size, the smaller the break the slower the depressurization. As a result, the primary system inventory is slowly depleted; and conditions of inadequate core cooling result before the accumulator actuation pressure is reached.
2. Beginning secondary steam-and-feed recovery operations after a condition of inadequate core cooling already exists can reduce the primary system pressure to the accumulator actuation pressure. In both Experiments S-NH-1 and S-NH-2, the secondary steam-and-feed recovery operations reduced the primary pressure to the accumulator actuation pressure after the PCT reached 811 K (1000°F). (Primary system inventory was 27%.) However, the accumulators emptied before the LPIS actuation pressure was reached; and, in Experiment S-NH-2 with the larger 2.1% break, an additional core thermal excursion was observed.
3. Beginning the secondary steam-and-feed recovery operations earlier in the transient can prevent any heater rod temperature excursion from occurring. In Experiment S-NH-5, the secondary steam-and-feed recovery operations were begun when the vessel liquid level reached the top of the core (primary system inventory at 62%); and no core thermal excursions were observed.
4. Restarting a reactor coolant pump under conditions of inadequate core cooling can help cool the core by forced convection. In Experiment S-NH-3, the intact loop coolant pump was restarted after the PCT had risen to 811 K (1000°F); and the core was quenched by the redistribution of the primary system mass. Liquid was in the

intact loop pump suction when the intact loop pump was restarted, and this liquid along with the liquid in the downcomer quenched the core when the intact loop pump was restarted. After the primary inventory dropped to 21%, core cooling was no longer adequate and the core began a second heatup. This heatup was much slower than the first, due to the forced convection as a result of the pump operation.

5. Fuel rod damage may already have occurred if recovery operations are not initiated until conditions of inadequate core cooling are detected. The only thermocouple near the core exit in the Semiscale facility remained well below the PCT during the heater rod temperature excursions. A better criterion for initiating recovery operations is using the average pressure difference between the primary system and the steam generator secondaries. The primary system pressure drops rapidly until the primary system inventory reaches 75%. This is not dependent on break size. The primary system pressure then becomes very stable as the primary inventory drops to 35%. The pressure at which the primary system pressure stabilizes is dependent on the steam generator secondary system pressures. The average pressure difference between the primary system and the steam generator secondaries as the primary system inventory decreased from 75% to 35% was about 0.5 MPa (72.5 psi).
6. The accumulators in all four experiments in the S-NH test series emptied before the LPIS actuation pressure was reached. If the primary system pressure is not sufficiently close to the LPIS actuation pressure when the accumulators empty, an additional core heatup requiring additional operator action may result.

REFERENCES

1. J. L. Anderson, Experiment Operating Specification for Semiscale Mod-2C Small Break LOCA without HPI Experiment Series, EGG-SEMI-6920, June 1985.
2. W. A. Owca, Appendix S-NH-1 and S-NH-2 of the Experiment Operating Specification for the Semiscale Mod-2C Small Break LOCA without HPI Experiment Series, EGG-RTH-7050, October 1985.
3. W. A. Owca, Quick Look Report for Semiscale Mod-2C Experiment S-NH-1, EGG-RTH-7147, February 1986.
4. J. E. Streit, Quick Look Report for Semiscale Mod-2C Experiment S-NH-2, EGG-RTH-7172, March 1986.
5. J. C. Chapman, Appendix S-NH-3 of the Experiment Operating Specification for the Semiscale Mod-2C Small Break LOCA without HPI Experiment Series, EGG-RTH-7119, December 1985.

6. J. C. Chapman, Quick Look Report for Semiscale Mod-2C Experiment S-NH-3, EGG-RTH-7232, May 1986.
7. W. A. Owca, Appendix S-NH-5 of the Experiment Operating Specification for the Semiscale Mod-2C Small Break LOCA without HPI Experiment Series, EGG-RTH-7140, January 1986.
8. J. E. Streit, Quick Look Report for Semiscale Mod-2C Experiment S-NH-5, EGG-RTH-7259, May 1986.
9. Zion Station Emergency Operating Procedure, "Inadequate Core Cooling," Rev. 1, approved December 10, 1981.
10. C. L. Nalezny, Summary of Nuclear Regulatory Commission's LOFT Program Experiments, NUREG/CR-3214, EGG-2248, July 1981.
11. T. K. Larson, J. L. Anderson, D. J. Shimeck, Scaling Criteria and an Assessment of Semiscale Mod-3 Scaling for Small Break Loss-of-Coolant Transients, EGG-SEMI-5121, March 1980.
12. G. G. Loomis and J. E. Streit, Results of Semiscale Mod-2C Small-Break (5%) Loss-of-Coolant Accident Experiments S-LH-1 and S-LH-2, NUREG/CR-24438 EGG-2424, November 1985.
13. J. P. Adams and G. E. McCreery, Detection of Inadequate Core Cooling with Core Exit Thermocouples: LOFT PWR Experience, NUREG/CR-3386, EGG-2260, November 1983.

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The Results of 5% Small-Break LOCA Tests
and Natural Circulation Tests at the ROSA-IV LSTF

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I. INTRODUCTION

The ROSA-IV Program at the Japan Atomic Energy Research Institute (JAERI) is conducting integral simulation experiments on pressurized water reactor (PWR) small-break loss-of-coolant accidents (SBLOCAs) and operational transients using the Large Scale Test Facility (LSTF).

The present paper summarizes results from several recent LSTF experiments which investigated the test parameter influences on 5%-break LOCA transients, as well as single-phase and two-phase natural circulation phenomena under steady-state conditions.

II. TEST FACILITY DESCRIPTION

The LSTF is a 1/48 volumetrically-scaled simulator of a Westinghouse-type 4-loop (3423 MWt) PWR [1]. The schematic view and design characteristics of the LSTF are shown in Fig. 1 and Table 1, respectively. The LSTF represents the four primary loops of the reference PWR with two equal-volume loops each including an active steam generator and a primary coolant pump. Major component elevations are preserved full scale to simulate correctly the natural circulation phenomena peculiar to SBLOCAs and transients. The hot legs and cold legs are sized to conserve the volume scaling ($2/48$) and the ratio of the length to the square root of the diameter, $L/\sqrt{D}$. Because of these scaling considerations and the large pipe diameter (207 mm) of the cold and hot legs, the LSTF is expected to simulate well the two-phase flow regime transition in the horizontal legs during a PWR LOCA.

III. TEST PARAMETER INFLUENCES ON 5%-BREAK LOCA TRANSIENT

Table 2 summarizes boundary conditions for the LSTF tests conducted to date with a break area of 5% of the scaled cold leg flow area. (This corresponds to an about 6-in break in the reference PWR.) All the tests were initiated from the PWR nominal operating pressure (15.5 MPa). The initial core power (10 MW) was smaller than the volumetrically-scaled PWR nominal core power, because of core power supply limitations. Then, to simulate the steady-state temperature distribution in the reference PWR primary system, power-scaled (14% of volumetrically-scaled) flow rates were established in the core and primary loops. Also, the primary-to-secondary temperature difference needed to be smaller than that in the reference PWR, to limit the steady-state secondary heat transfer rate at 10 MW. Thus, the steady-state secondary-side pressure was 7.4 MPa, whereas it is 6.2 MPa in the reference PWR.

The tests were initiated by opening a break valve. At the same time, the primary coolant pump speed was increased quickly to simulate the hot leg temperature drop in the reference PWR following scram. (Without this procedure, the hot leg temperature drop in the LSTF is much slower than in

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the reference PWR, since the initial core velocity is 14% of that in the reference PWR [2].)

Major operational setpoints for the tests are shown in Fig. 2. At a pressurizer pressure of 12.97 MPa, a core power trip (scram) signal was generated. The scram signal initiated the core power decay and closed the main steam isolation valve. It also initiated coastdown of the primary coolant pump speed and tripped off the main feedwater, since the loss of offsite power concurrent with the reactor scram was assumed. The same core power decay curve was used for all the tests except for SB-CL-06 (Fig. 3).

At a pressurizer pressure of 12.27 MPa, a safety injection (SI) signal was sent. The LSTF ECCS consists of a charging pump, a high pressure injection (HPI) pump, two accumulator tanks, and a low pressure injection (LPI) pump. The ECCS injection was initiated when the primary system pressure reached the simulated ECCS pump injection pressures. The flow vs. head characteristics of ECCS pumps in the reference PWR were simulated (Fig. 4). The accumulator injection began when the primary system pressure became lower than 4.5 MPa.

III.1 Cold-Leg Breaks

All the cold-leg break tests simulated a side break on the cold-leg horizontal pipe in Loop B (the loop without the pressurizer). Figure 5 shows the primary and secondary system pressure responses during the reference cold-leg break tests SB-CL-05. The primary system pressure remained higher than the secondary system pressure until the loop seal cleared at about 140 s after break. The loop seal clearing caused the break flow to change from low-quality two-phase flow to vapor, and thus caused the primary system to depressurize rapidly.

Figure 6 compares core collapsed liquid levels and typical rod temperature responses obtained in the four 5% cold-leg break tests. The response of the core level during a LOCA is dependent on two factors: (i) the vessel residual mass inventory and (ii) the differential pressure between the upper plenum and the downcomer. For a cold leg break LOCA, the upper plenum-to-downcomer differential pressure maximizes during the clearing of the loop seal, when the loop seal differential pressure maximizes to be equal to the gravitational head of a liquid column filling the loop seal upflow side. This maximum loop seal differential pressure can depress the core collapsed level down to the loop seal bottom elevation, whereas the downcomer is full of liquid up to the cold legs.

SB-CL-05 had a core level depression reaching considerably below the loop seal bottom elevation as shown in Fig. 6. This resulted from the additional differential pressure due to a larger liquid holdup in the steam generator (SG) U-tube upflow side than in the downflow side (Fig. 7) [3]. After the loop seal cleared, the differential pressure was relieved so that the core level recovered quickly. The accumulator injection began at 417 s and arrested the decrease of the primary system mass inventory.

SB-CL-06, a counterpart test for the Semiscale test S-LH-1 [4], was conducted for a smaller core power than the reference tests SB-CL-05 (Fig. 3), and had two bypasses between the upper plenum and the downcomer: one through the upper head spray nozzles providing a bypass flow of 0.9% of the total core flow rate, and another connecting directly the downcomer and the upper plenum providing a 0.6% bypass flow (vs. 0.2% in SB-CL-05). Semiscale S-LH-1 had a 0.9% bypass flow through the upper head alone. SB-CL-06 indicated a smaller core level depression than SB-CL-05 (Fig. 6-a)

as the U-tubes were almost empty of liquid at the time of loop seal clearing, which occurred at 208 s (vs. 140 s in SB-CL-05).

SB-CL-07 simulated the operation of Babcock and Wilcox (B&W)-type core barrel vent valves since 60 s into the transient. The valve flow area was smaller than the scaled total flow area of the prototypical B&W core barrel vent valves. The opening of the vent valve created a path for a vapor flow from the upper plenum to the downcomer, equivalent to a 8.3% bypass. This vapor flow caused the cold leg fluid to saturate quickly, and thus the break flow to turn to relatively-high quality two-phase flow early in the transient. However, the transition of break flow to vapor did not occur until late in the transient, because the loop seal clearing delayed. As a result, the system depressurization delayed, and the accumulator injection initiated later than in SB-CL-05. The continued decrease of the vessel mass inventory caused the core top region to uncover until the loop seal finally cleared (Fig. 6-b). The loop seal clearing delayed in SB-CL-07 because the vapor flow through the vent valve, as well as the vaporization of the cold leg fluid, prevented the upper plenum-to-downcomer differential pressure to develop sufficiently to clear the loop seal.

SB-CL-08 simulated the total failure of the high pressure charging and injection pumps, as well as the failure of the auxiliary feedwater system, with other test conditions almost the same as SB-CL-05. The failure of the high-pressure ECCS had no significant influence until the loop seal clearing as shown in Fig. 6-c, because the lack of ECC injection resulted in higher fluid specific enthalpy in the cold leg and the break flow became smaller than SB-CL-05 compensating no replenishment of mass inventory by ECCS in SB-CL-08. After the loop seal clearing, the core level response in SB-CL-08 differed from that in SB-CL-05, since the primary mass inventory continued to decrease. Thus, the top of core uncovered again until the accumulators came on.

III.2 Breaks at Other Locations

Figure 8 shows the effect of break location on the core liquid level and the rod temperature response.

SB-IS-01 simulated a side break at the bottom of the loop seal in Loop B. This test indicated qualitatively the same overall response as that in the cold leg break tests. However, the break flow rate was smaller than in SB-CL-05, since the break upstream void fraction became large before the loop seal cleared. Thus, the decrease of mass inventory was slower, and the loop seal clearing occurred later than in SB-CL-05. At the time of loop seal clearing the SG U-tubes were empty of liquid, so that the minimum core level was almost equal to the loop seal bottom elevation.

SB-HL-01 simulated a side break on the hot-leg horizontal pipe in Loop B, followed by the total failure of high pressure charging and injection pumps. In this test two-phase discharge continued until about 350 s, considerably later than in the reference cold-leg break test SB-CL-05. Vapor discharge began as the SG inlet plenum became empty of liquid, and caused the primary system to depressurize, so that the cold leg fluid became saturated. After the cold leg saturation the cold leg became empty of liquid and the downcomer level dropped as low as the core inlet elevation, as the downcomer pressure became higher than the upper plenum pressure (Fig. 9). Then, the accumulator injection caused condensation of vapor and depressurization in the cold leg. Fluid in the downcomer was

sucked up by this depressurization, and therefore a temporary but steep depression of the core liquid level occurred.

IV. NATURAL CIRCULATION TESTS

IV.1 Primary Loop Cooling Modes

Two natural circulation tests were conducted for 5% and 2% scaled nominal PWR core power respectively (Table 3). These tests consisted of many steady-state stages obtained for different values of the primary mass inventory, which was decreased stepwise from the 100% initial value by draining liquid from the bottom of the pressure vessel. The secondary pressure and water level were maintained at constant values close to the PWR nominal operating conditions (6.5 MPa and 9.5 m, respectively).

The change of primary loop flow rate with the decrease of mass inventory (Fig. 10) was qualitatively the same as observed on other facilities, e.g., Semiscale [5], LOBI [6] and PKL [7]. Two-phase natural circulation initiated when the mass inventory decreased to about 90%, as vapor began to enter the hot legs. This resulted in an increase of the loop flow rate until the loop flow rate took the maximum values at a 76% mass inventory for the 5% core power, and at a 81% mass inventory for the 2% core power, respectively.

Temperature measurements inside the U-tubes indicated that flow in longer U-tubes was stagnant or reversed in direction for mass inventories greater than 80%, probably because the SG outlet plenum pressure was higher than the inlet plenum pressure. This situation can occur when the gravitational head difference between the U-tube upflow side and downflow side is greater than the irreversible pressure drop. Although the flow was stagnant or reversed in many of the U-tubes, the primary system was kept well cooled by the primary-to-secondary heat transfer through the SGs. The primary fluid was cooled as it flowed through the normal U-tubes, and the SG outlet temperature was always almost equal to the secondary-side temperature.

The loop flow rate during the two-phase natural circulation was fairly steady and did not exhibit any such large oscillations as observed in smaller-scale facilities. Although flow in individual U-tubes was intermittent, the overall loop flow was steady because of the large number of U-tubes (141 per SG) in the LSTF.

Further reduction of the primary mass inventory resulted in a steep decrease in the loop flow rate, as stationary vapor bubbles began to form at the top of U-tubes. The two-phase natural circulation ceased when the mass inventory decreased to 56% for the 5% core power, and 64% for the 2% core power. Then the reflux condensation cooling mode followed. The core remained submerged and cooled until the mass inventory decreased to 30% for the 2% core power.

IV.2 Steam Generator Overall Heat Transfer Coefficients

After the completion of the primary-side natural circulation tests, the SG heat transfer coefficient was measured for reduced secondary-side mass inventories, while the primary side was kept in the reflux condensation mode [8]. The SG effective overall heat transfer coefficient was defined for the U-tube secondary-side heat transfer area below the secondary-side collapsed liquid level, the primary-to-secondary temperature difference, and the core power. The heat transfer coefficients were

weakly dependent on the secondary side liquid level and the core power as shown in Fig. 11. The heat transfer coefficient increased with decrease of the liquid level. The measured minimum heat transfer coefficient (recorded for the maximum secondary level) were $2.6 \text{ kW/m}^2/\text{K}$ for the 5% core power and $1.7 \text{ kW/m}^2/\text{K}$ for the 2% core power.

V. REFERENCES

1. ROSA-IV Group, "ROSA-IV Program Large Scale Test Facility (LSTF) System Description," JAERI-M 84-237 (1985).
2. Kukita, Y. et al., "ROSA-IV Program LSTF Pretest Analysis: Stratification Capability in SBLOCA Tests," 1985 Japan Atomic Energy Society Annual Meeting.
3. Oszkabe, M. et al., "Thermal-Hydraulic Mechanisms of Core Liquid Level Depression and Recovery During Small Break LOCA Experiment," Proc. Int. ANS/ENS Topical Mtg. Thermal Reactor Safety, San Diego, California, Feb. 2-6, 1986.
4. Loomis, G. and Streit, J. E., "Results of Semiscale Mod-2C Small-Break (5%) Loss-of-Coolant Accident Experiments S-LH-1 and S-LH-2," NUREG/CR-4438 EGG-2424 (1985).
5. Loomis, G. G. and Soda, K., "Results of the Semiscale Mod-2A Natural Circulation Experiments," NUREG/CR-2335, EGG-2200 (1982).
6. De Santi, G. F., Piplies, L. and Riebold, W. L., "Analysis of Natural Circulation Phenomena in the LOBI-MOD2 Facility," Proc. Int. ANS/ENS Topical Mtg. Thermal Reactor Safety, San Diego, California, Feb. 2-6, 1986.
7. Mandl, R. M. and Weiss, P. A., "PKL Tests on Energy Transfer Mechanisms During Small-Break LOCA," Nuclear Safety 23-2 (1982).
8. Kumamaru, H. et al., "Effect of Steam Generator Secondary Inventory on Reflux Condensation," to be presented at the 1986 ASME Winter Annual Meeting (1986).

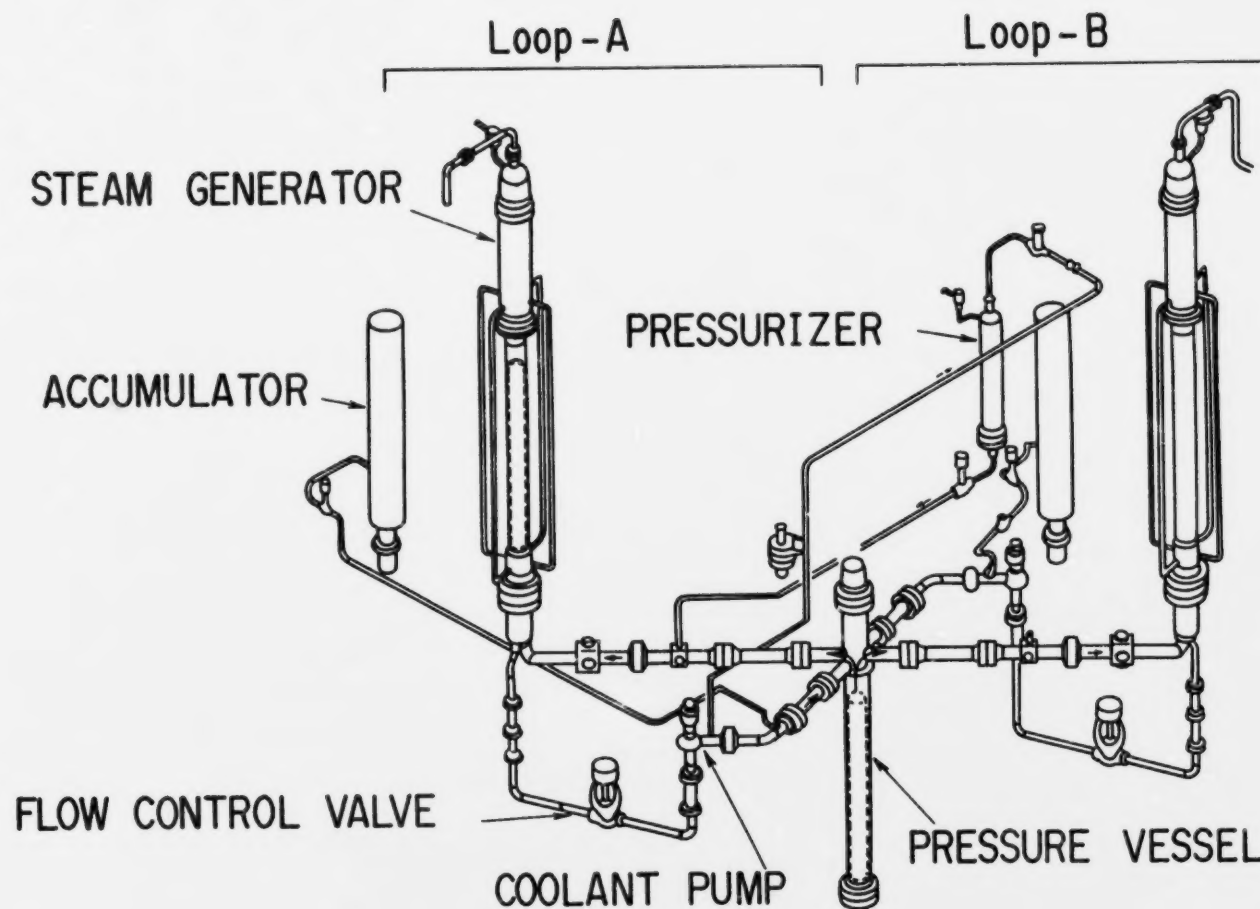


Fig. 1 ROSA-IV Large Scale Test Facility.

Table I Major Design Characteristics of LSTF and PWR

		LSTF	PWR	PWR/LSTF
Pressure	(MPa)	16	16	1
Temperature	(K)	598	598	1
No. of fuel rods		1064	50952	48
Core height	(m)	3.66	3.66	1
Fluid volume V	(m ³)	7.23	347	48
Core power P	(MW)	10	3423(t)	342
P/V	(MW/m ³)	1.4	9.9	7.1
Core inlet flow	(t/s)	0.0488	16.7	342
Downcomer gap	(m)	0.053	0.260	4.91
Hot leg D	(m)	0.207	0.737	3.56
L	(m)	3.69	6.99	1.89
$L/\sqrt{D}$	(m ^{1/2})	8.15	8.15	1.0
$\frac{\pi}{4}D^2L$	(m ³)	0.124	2.98	24.0
No. of loops		2	4	2
No. of tubes in steam generator		141	3382	24
Length of steam generator tube (average)	(m)	20.2	20.2	1.0

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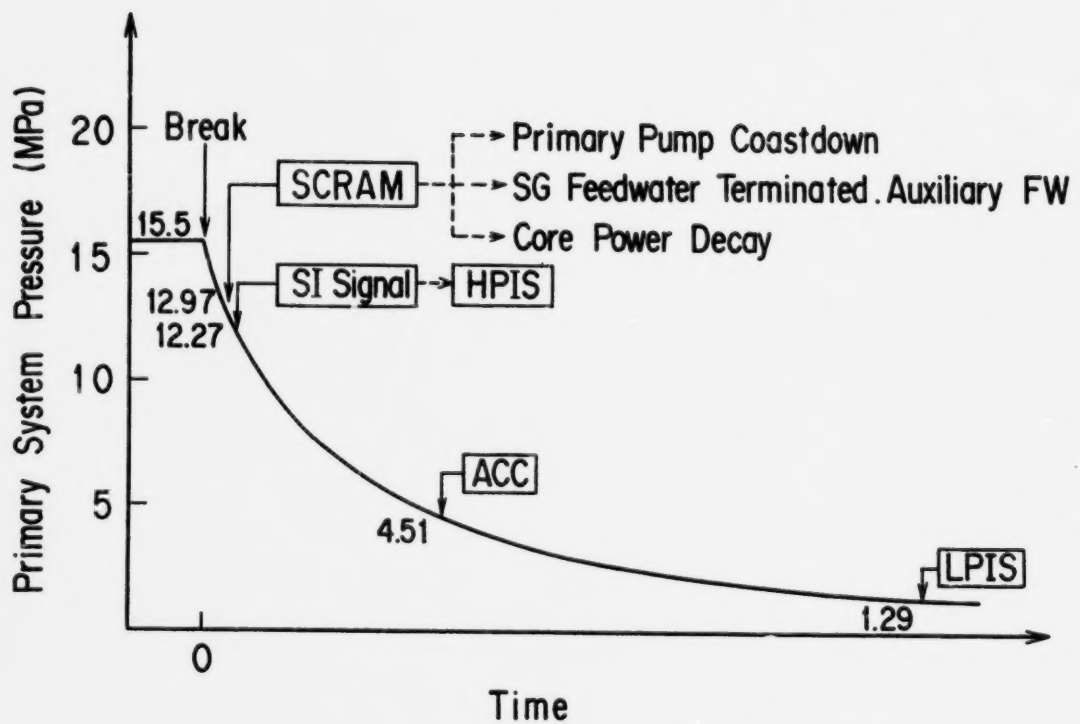


Fig. 2 Major Operational Setpoints for LOCA Tests.

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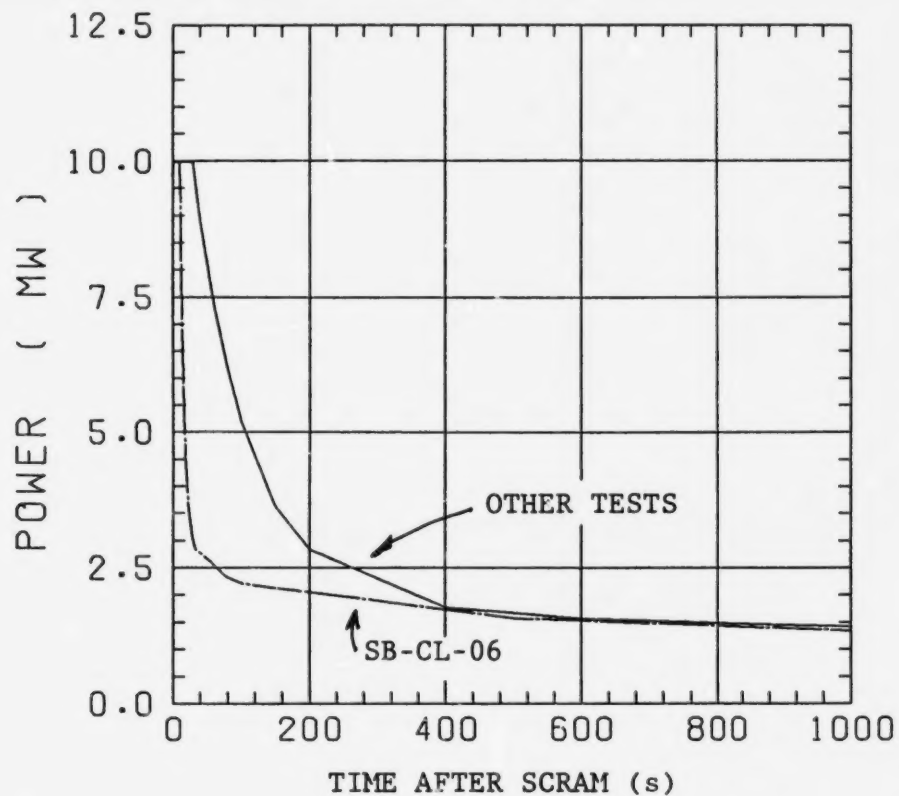


Fig. 3 Core Power Decay Curves

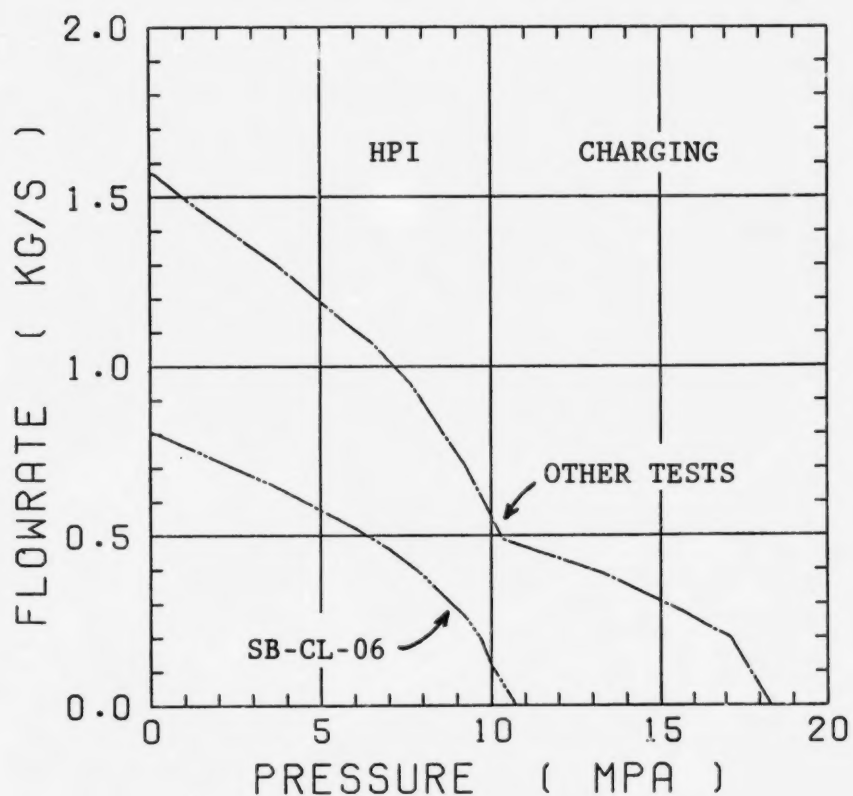


Fig. 4 ECCS Flow vs. Head Characteristics

Table 2 5% Break Test Conditions

Test ID	SB-CL-05	SB-CL-06	SB-CL-07	SB-CL-08	SB-HL-01	SB-LS-01
Break Location	Cold Leg	Cold Leg	Cold Leg	Cold Leg	Hot Leg	Loop Seal (Bottom)
Bypass (% of core flow)						
Downcomer-Hot Leg	0.2	0.6	0.0	0.2	0.2	0.2
Downcomer-Upper Head	2.1	0.9	0.9	0.3	0.3	0.9
Internal Vent Valve	0.0	0.0	8.3	0.0	0.0	0.0
High-Pressure ECCS	Single Train	Single Train	Single Train	None	None	Single Train

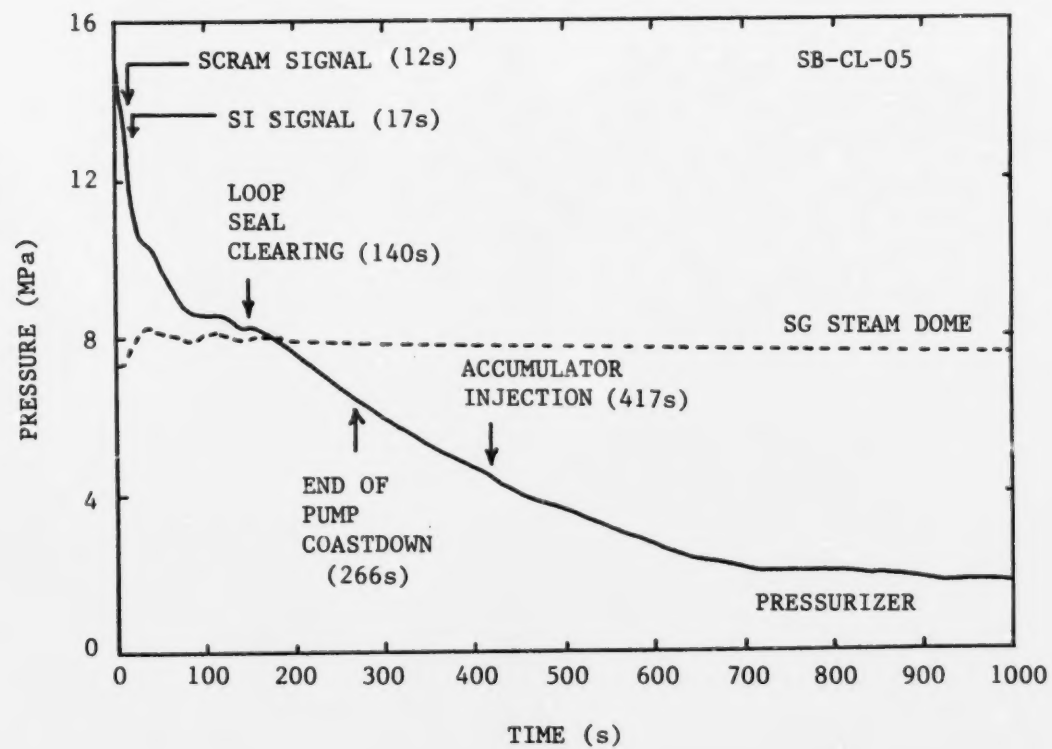


Fig. 5 Primary and Secondary System Pressures During Reference Cold-Leg Break Test SB-CL-05

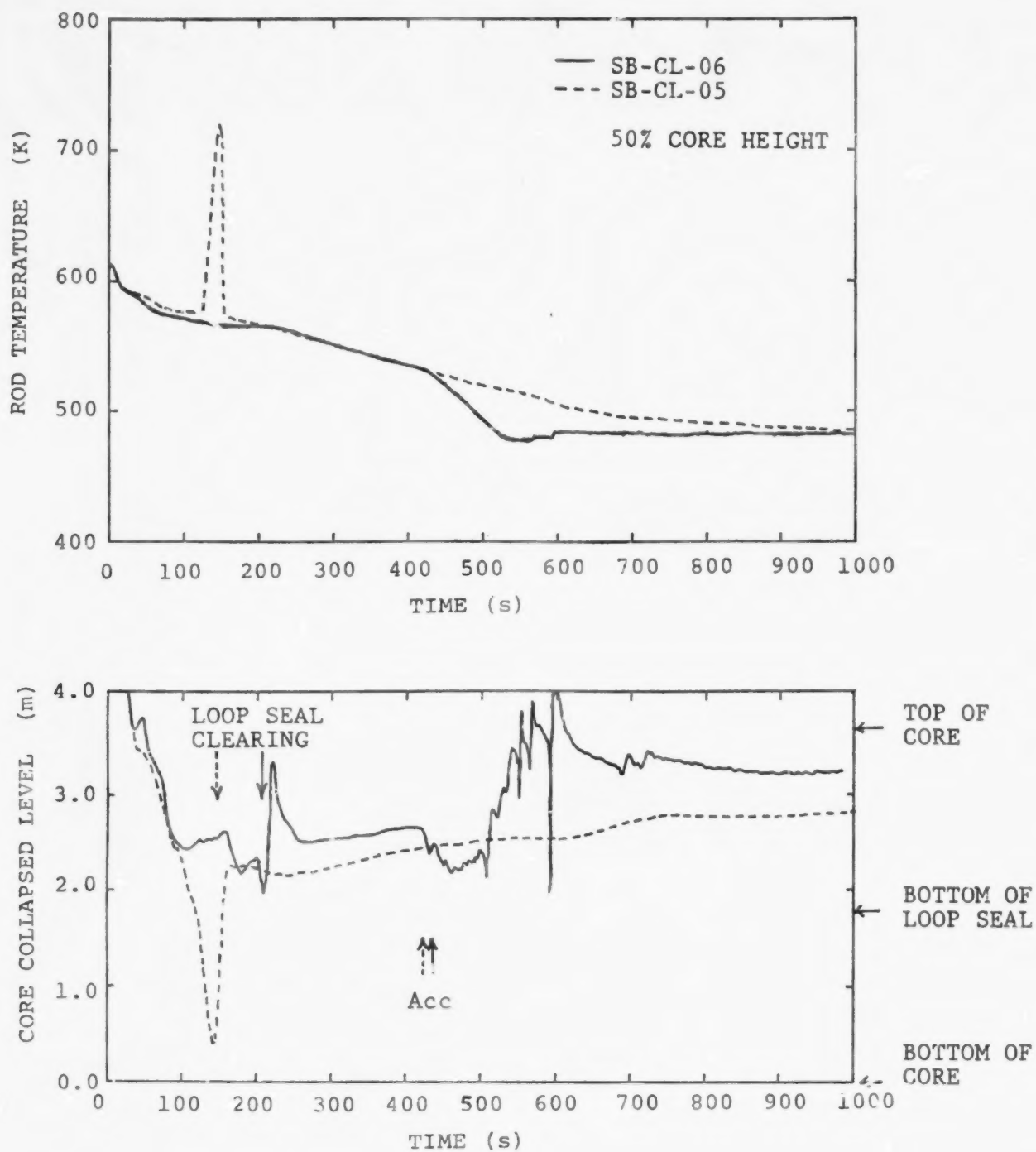


Fig. 6-a Rod Temperature Response and Core Collapsed Liquid Level Response, Comparison of SB-CL-06 and SB-CL-05 Test Results.

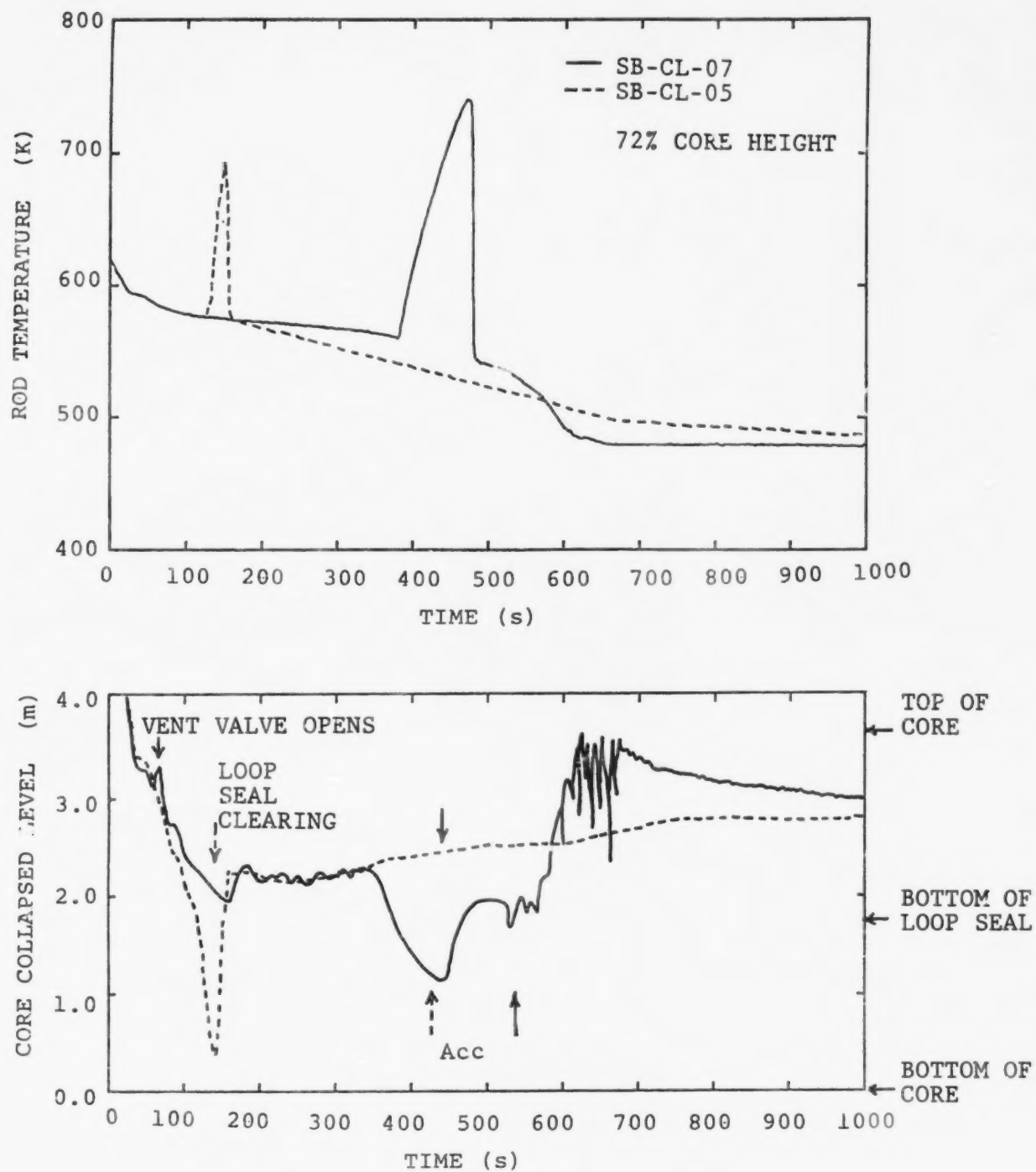


Fig. 6-b Rod Temperature Response and Core Collapsed Liquid Level Response, Comparison of SB-CL-07 and SB-CL-05 Test Results.

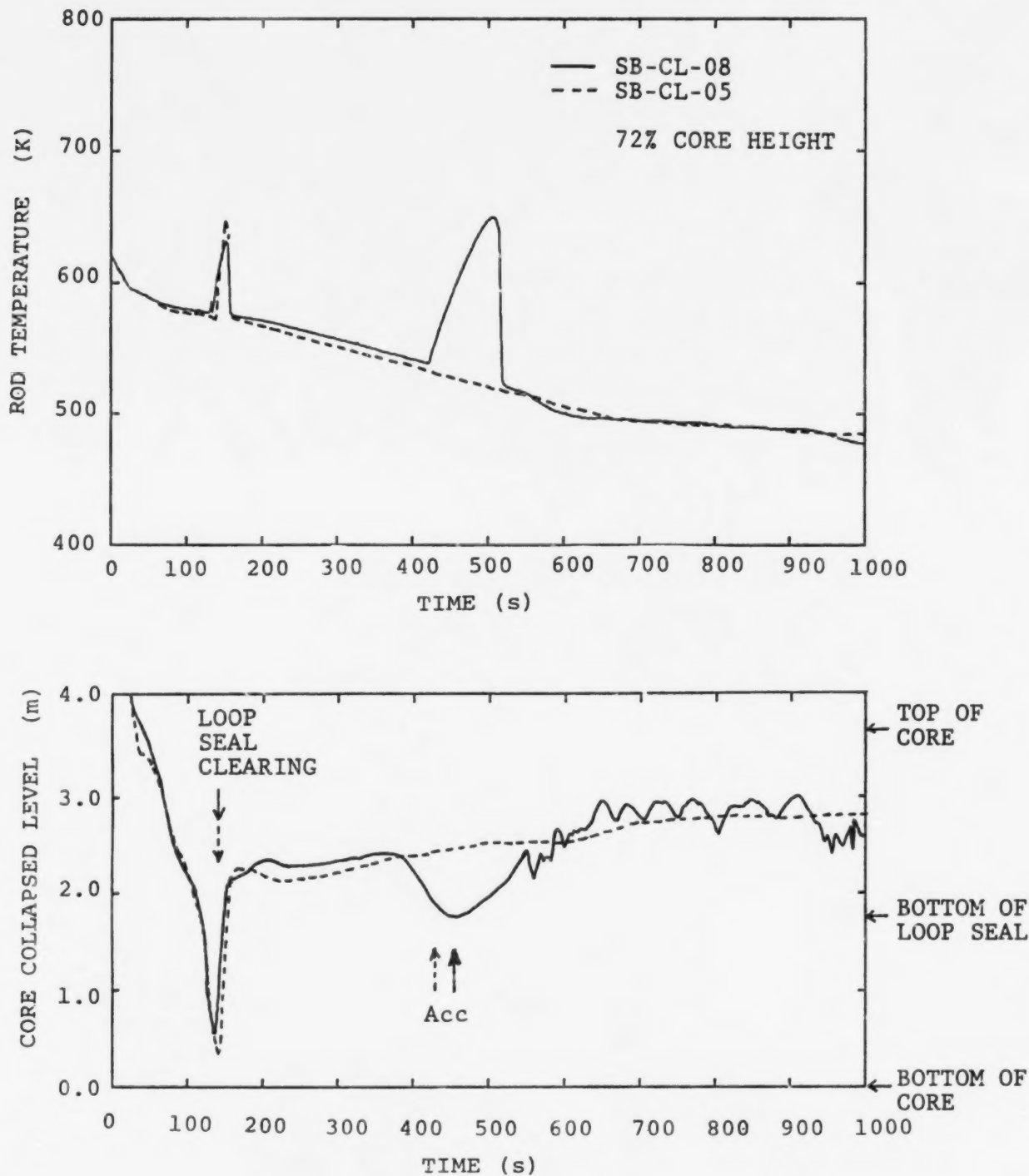


Fig. 6-c Rod Temperature Response and Core Collapsed Liquid Level Response, Comparison of SB-CL-08 and SB-CL-05 Test Results.

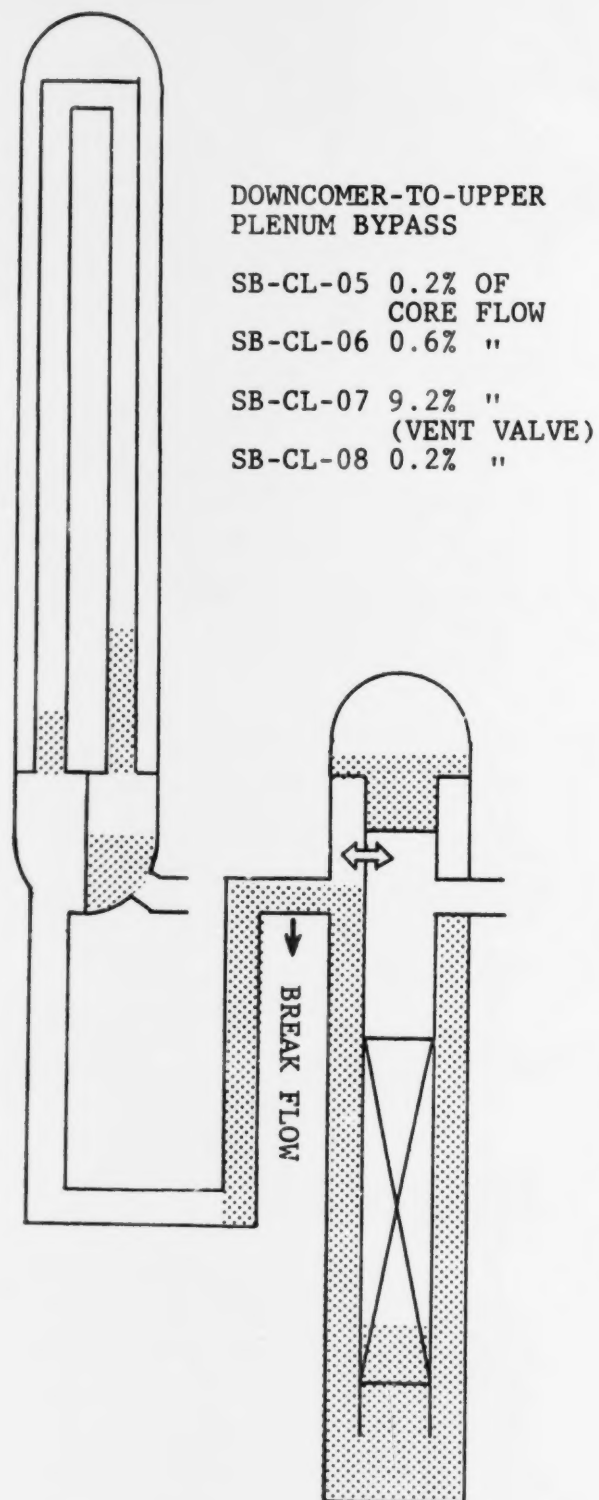


Fig. 7 Collapsed Liquid Level Distribution During Loop Seal Clearing in Cold-Leg Break Tests.

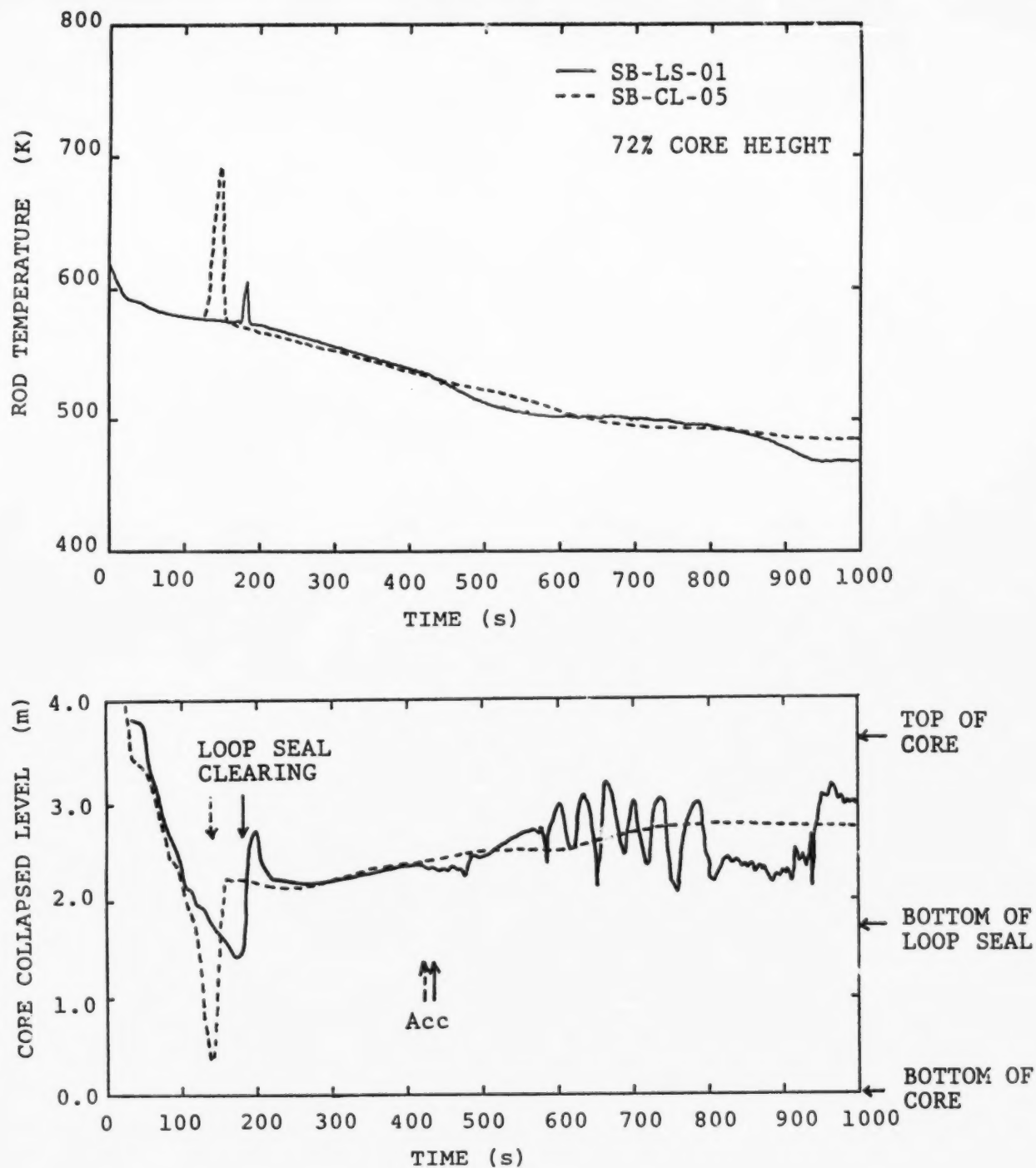


Fig. 8-a Rod Temperature Response and Core Collapsed Liquid Level Response, Comparison of SB-LS-01 and SB-CL-05 Test Results.

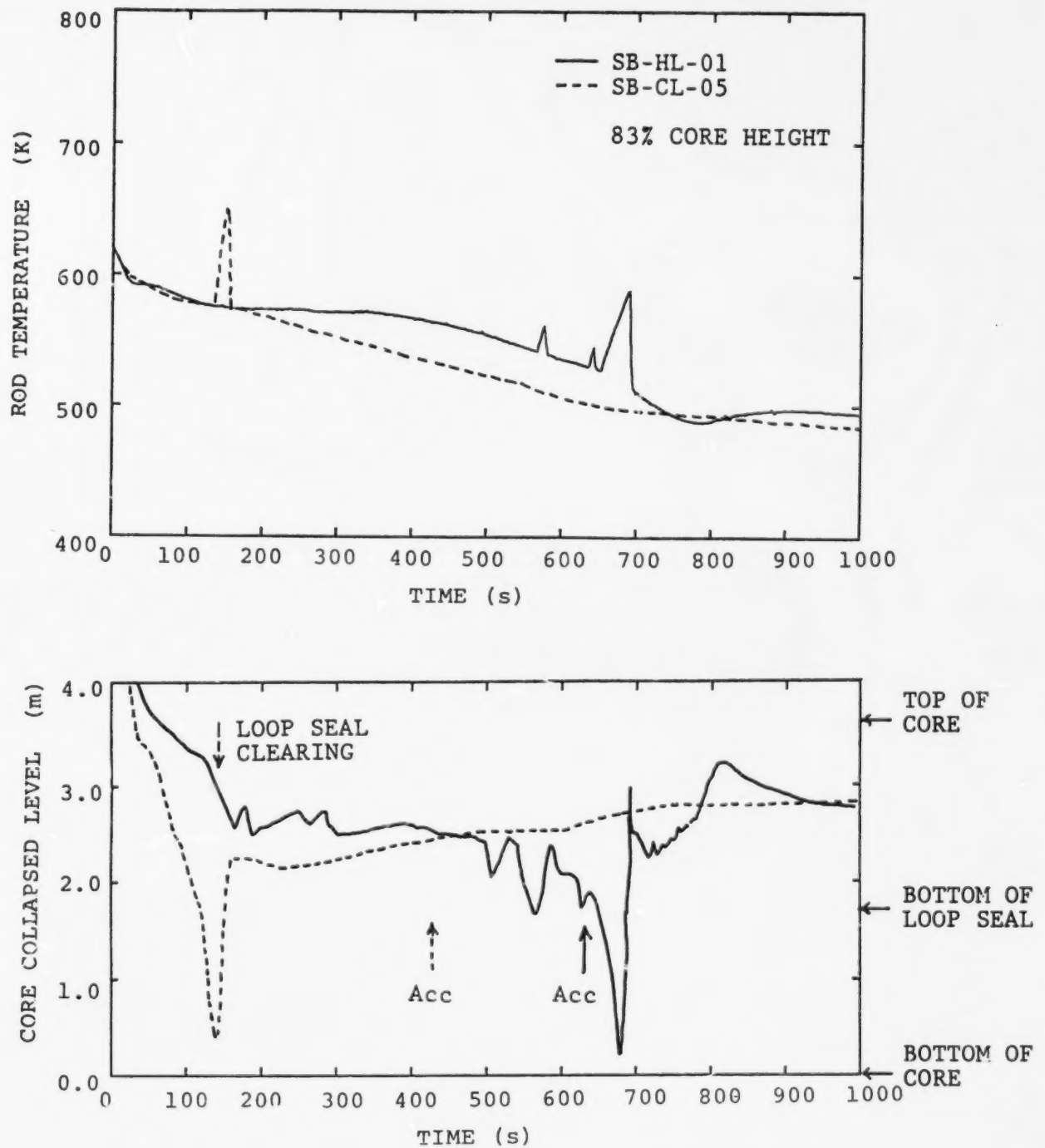


Fig. 8-b Rod Temperature Response and Core Collapsed Liquid Level Response, Comparison of SB-HL-01 and SB-CL-05 Test Results.

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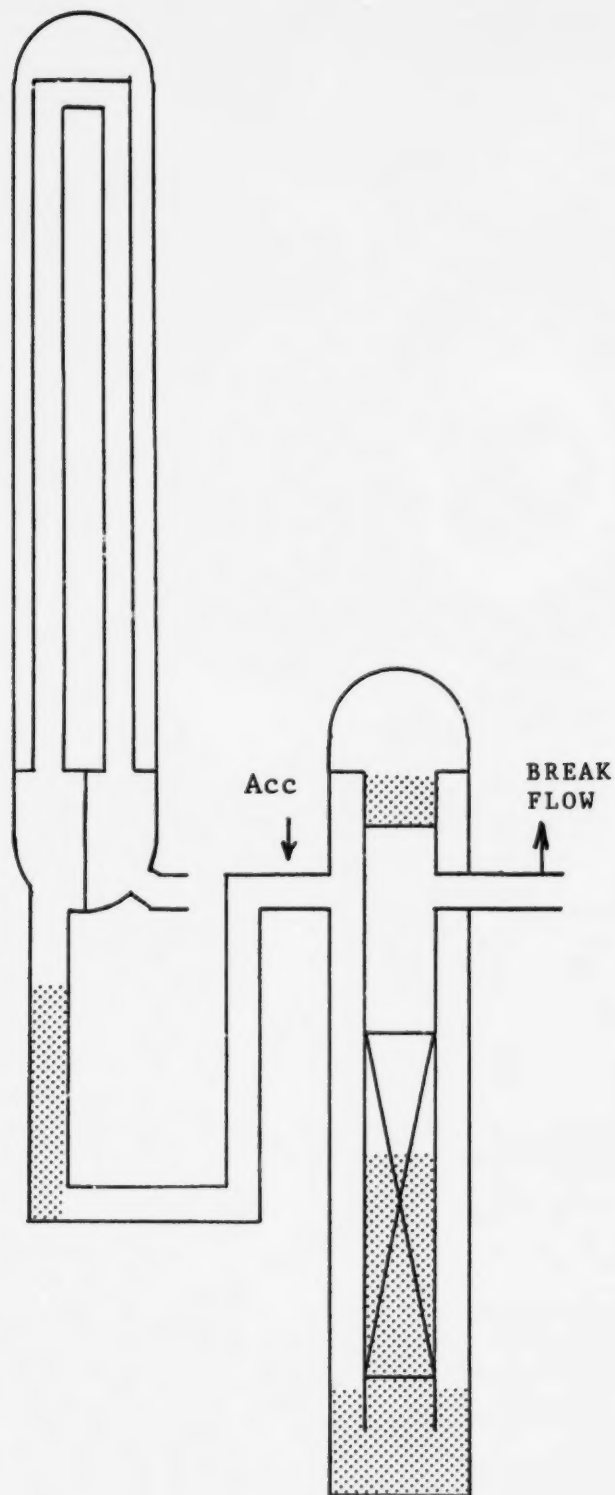


Fig. 9 Collapsed Liquid Level Distribution
Before Accumulator Injection in Hot-Leg
Break Test SB-HL-01.

Table 3 Natural Circulation Test Conditions

Test ID	ST-NC-01 /ST-SG-01	ST-NC-02 /ST-SG-02
Core Power (MW/%)	3.56/5	1.42/2
Pressure		
Primary System (MPa)	7.2 - 15.5	6.8 - 15.5
Secondary System (MPa)	6.5	6.5

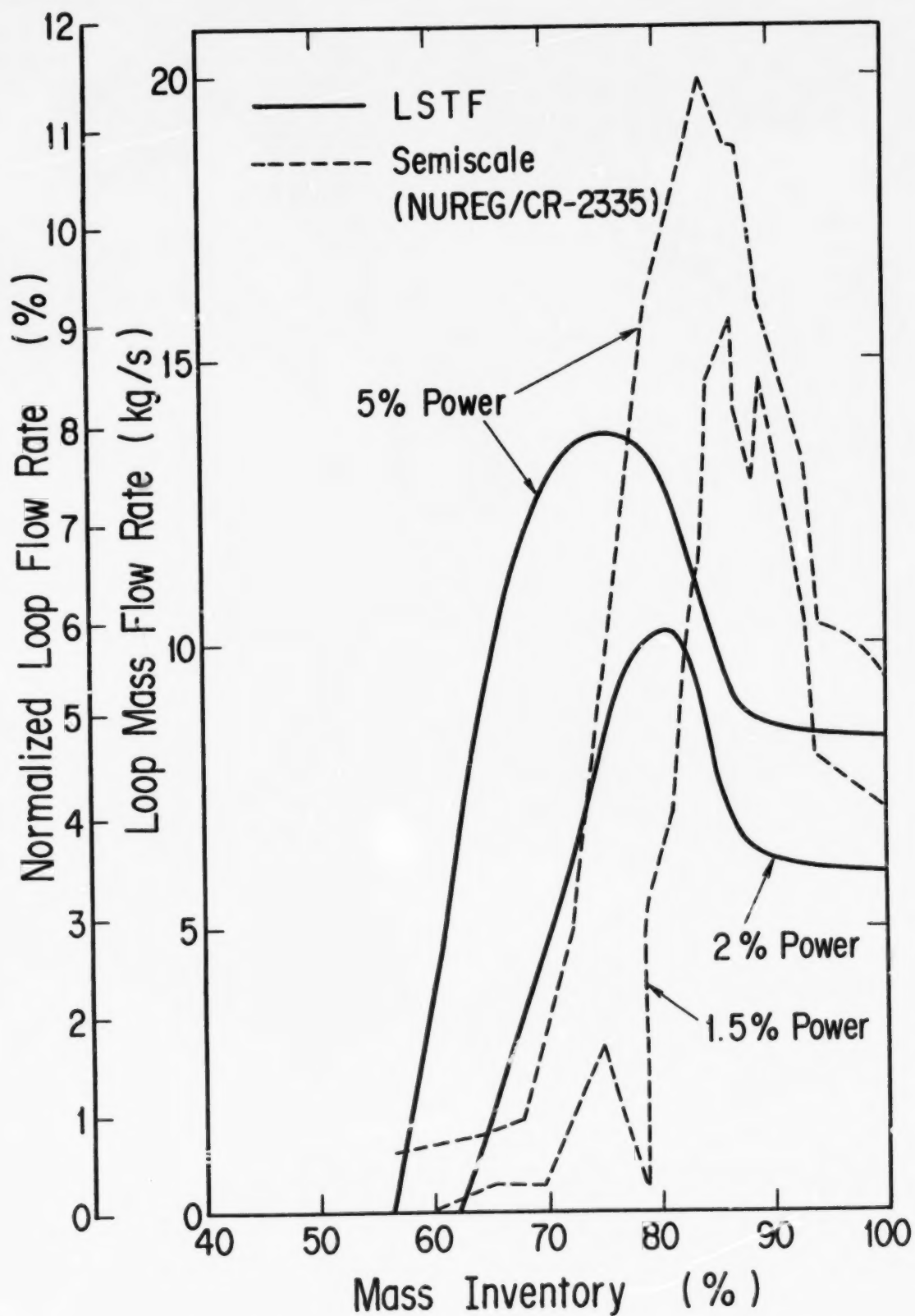


Fig. 10 Primary Loop Natural Circulation Flow Rate, Comparison of LSTF and Semiscale Test Results.

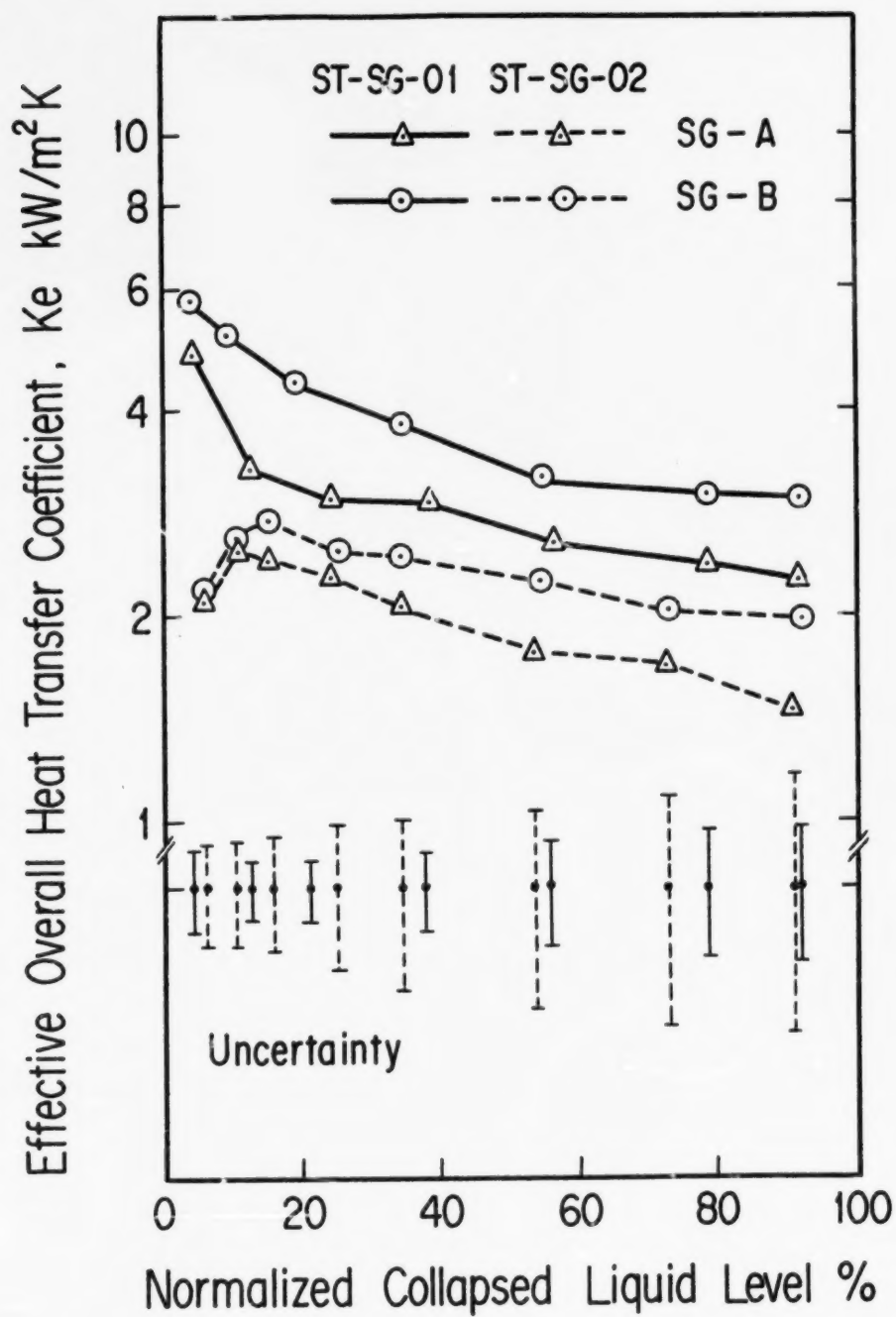


Fig. 11 Steam Generator Overall Heat Transfer Coefficient.

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COMPARISON OF A TRAC CALCULATION TO THE DATA FROM LSTF RUN SB-CL-05*

by

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ABSTRACT

Run SB-CL-05 is a 5% break in the side of the cold leg. The test results show that the core was uncovered briefly and that the rods overheated at certain core locations. Liquid holdup on the upflow side of the steam generator tubes was observed. When the loop seal cleared, the core refilled and the rods cooled.

The TRAC results are in reasonable agreement with the test data, meaning that TRAC correctly predicted the major trends and phenomena. TRAC predicted the core uncover, the resulting rod heatup, and the liquid holdup on the upflow side of the steam generator tubes correctly. The clearing of the loop seal allowed core recovery and cooled the overheated rods just as it had in the data, but TRAC predicted its occurrence 20 s late.

The experimental and TRAC analysis results of run SB-CL-05 are similar to those for Semiscale Run S-UT-8. In both runs there was core uncover, rod overheating, and steam generator liquid holdup. These results confirm scaling of these phenomena from Semiscale (1/1650) to LSTF (1/48).

I. INTRODUCTION

Los Alamos National Laboratory and Idaho National Engineering Laboratory (INEL) are analyzing selected Large-Scale Test Facility (LSTF) experiments using the Transient Reactor Analysis Code (TRAC),¹ developed by Los Alamos. This report describes the results of the first LSTF test analyzed using TRAC. The LSTF is a large-scale (1/48) integral-test facility for the study of pressurized water reactors (PWRs) during small-break loss-of-coolant accidents (SBLOCA) and anticipated reactor transients. All the major components of the primary and secondary systems of a PWR are modeled by the LSTF. The available power is limited to 14% of scaled full power so that for steady-state operation the loop flow is reduced to maintain

* Work performed under the auspices of the US Nuclear Regulatory Commission

the correct temperature rise through the heated core. The secondary pressure is increased to reduce the primary-to-secondary heat transfer to 14%. The LSTF was built by the Japan Atomic Energy Research Institute (JAERI) and the test results are shared with the US Nuclear Regulatory Commission (NRC) according to terms of a bilateral agreement.

The main objective of Run SB-CL-05 was to investigate the thermal-hydraulic mechanism of early core uncover and heatup caused by manometric imbalance due to liquid holdup in the upflow side of the tubes of the steam generators.² The results show that the test demonstrated all of the phenomena expected. The core uncovered briefly and the rods overheated at certain core locations. The overheating was limited to 100 K. Liquid holdup was observed in the upflow side of the steam generator tubes. When the loop seal cleared, the core refilled and the heater rods cooled. Throughout the remainder of the test the core was cool. Because this test was one of the first performed in the LSTF, some test instruments failed and others were subject to errors.

Early core uncover and heater rod heatup were first observed in the Semiscale Mod-2a test facility in Run S-UT-8.³ These results were unexpected and further tests were conducted in Semiscale to study these phenomena.⁴ Liquid holdup and core uncovers were observed in the additional tests. The small scale of the Semiscale facility (1/1650) was of concern in applying the test results to PWRs.

II. TRAC MODEL DESCRIPTION

The TRAC input model of the LSTF is very detailed and describes the primary system and the secondary system to the steam valves.⁵ The emergency core-cooling system (ECCS) is modeled as a boundary condition that includes high-pressure injection (HPI), accumulator, and low-pressure injection (LPI) flows. Figure 1 shows the TRAC model of the LSTF. There are 41 components and 50 junctions. The detailed vessel model is shown in Fig. 2. There are 325 computational cells in the model. This noding is considered very detailed for analysis of a SBLOCA. The three inner rings of the vessel allow representation of each of the radial power zones in the core. The six core levels accurately model the axial heat flux shape. Six pipes model the guide tubes which connect the upper head (three top levels) to the upper plenum. The detailed description of the TRAC input model is contained in Ref. 6. TRAC-PF1/MOD1 version 12.7 was used for this calculation.

III. INITIAL AND BOUNDARY CONDITIONS

Steady-state initial conditions for the LSTF are similar to normal operating conditions in a PWR except that the loop flow and the core power are reduced to 14% of the scaled PWR values. This is necessary to maintain the correct temperature rise in the core with the limited power available in the LSTF. The secondary-side pressure in the steam generators was increased to reduce the primary-to-secondary heat transfer. The comparison of measured to calculated initial conditions is shown in Table I. There is good agreement in all comparisons. The calculated hot-leg temperatures are slightly high because of the lower loop flow and the heat losses in the test facility that were not accounted for in the TRAC model. The heat losses are not considered to be important for this transient. The core bypass flow was based on the results of a special leakage test. All the bypass in the vessel is assumed to occur between the downcomer and the upper head.

The boundary conditions imposed during the transient were taken from the test data. The power to the core is held at the steady-state level until 45 s after the break opens; then it follows a calculated radioactive decay. The pump speed is increased rapidly after the break opens until it doubles at 15 s; then it is controlled to model pump coastdown. This procedure is an attempt to pick up the transient as if it had started at full power and flow conditions. The ECCS flows into the system were summed and provided to the ECCS locations in the input model. The steam-generator secondary main steam valves close at 15 s. The secondary-side pressure is controlled by the relief valves, which are set to open at 8.03 MPa and to close at 7.82 MPa. The steam-generator feedwater flow is supplied to the calculation in the same manner as during the test.

IV. DEFINITIONS OF COMPARISON DESCRIPTORS

The agreement between the data and the TRAC-calculated results is characterized by four descriptors: *excellent agreement*, *reasonable agreement*, *minimum agreement*, and *insufficient agreement*. Each descriptor is defined in the following paragraphs, along with the consequences for future application of the code in the given area being characterized and the perceived need for additional code development.

Excellent agreement is an appropriate descriptor when the code exhibits no deficiencies in modeling a given behavior. Major and minor phenomena and trends are predicted correctly. The calculated results are judged by the analyst to be close to the data with which a comparison is being made. If the uncertainty of the data has been identified and made available to the analyst, the calculation will, with few exceptions, lie within the uncertainty band of the data. The code may be used with confidence in similar applications. Neither code models nor the facility nodding model requires examination or change.

Reasonable agreement is an appropriate descriptor when the code exhibits deficiencies, but the deficiencies are minor; that is, the deficiencies are acceptable because the code provides an acceptable prediction of the test. All major trends and phenomena are predicted correctly. Differences between the test and calculated traces of parameters identified as important by the analyst are greater than those deemed necessary for excellent agreement. If uncertainty data are available, the calculation frequently will lie outside the uncertainty band. However, the analyst believes that the discrepancies are insufficiently large to require a warning to potential users of the code in similar applications. The assessment analyst believes that the correct conclusions about trends and phenomena would be reached if the code were used in similar applications. The code models and/or facility nodding model should be reviewed to see whether improvements can be made.

Minimal agreement is an appropriate descriptor when the code exhibits deficiencies and the deficiencies are significant; that is, the deficiencies are such that the code provides a prediction of the test that is only conditionally acceptable. Some major trends or phenomena are not predicted correctly whereas others are predicted correctly. Some TRAC-calculated values lie far outside the uncertainty band of the data with which a comparison is being made. The assessment analyst believes that incorrect conclusions about trends and phenomena might be reached if the code were used in similar applications. The analyst believes that certain code models and/or the facility nodding model must be reviewed, corrections made, and a limited assessment of the revised code or input models made before the code can be used with confidence for similar applications. A warning should be issued to the TRAC user

community that the user applying the code in similar applications risks drawing incorrect conclusions. This warning should stay in force until the identified review, modification, and limited assessment activities are completed and the resultant characterization descriptor is "reasonable" or better.

Insufficient agreement is an appropriate descriptor when the code exhibits major deficiencies; that is, the deficiencies are such that the code provides a prediction of the test that is unacceptable. Major trends are not predicted correctly. Most TRAC-calculated values lie far outside the uncertainty band of the data with which a comparison is being made. The assessment analyst believes that incorrect conclusions about trends and phenomena are probable if the code is used in similar applications. The analyst believes that certain code models and/or the facility nodding model must be reviewed, corrections made, and a limited assessment of the revised code or facility nodding model made before the code can be used with confidence for similar applications. A warning should be issued to the TRAC user community that the code must not be used for similar applications until the identified review, modification, and limited assessment activities are completed and the resultant characterization descriptor is "reasonable" or better.

V. COMPARISON OF TRAC-CALCULATED RESULTS TO LSTF DATA

The TRAC calculation proceeds in a manner similar to the experiment. The comparison of the sequence of events is shown in Table II. Most of the small differences in timing between the test data and the TRAC calculation can be explained by the fact that the test data are from Ref. 2 and the TRAC input was taken from the data tape. The major difference is the delay of 20 s in the core uncover and loop seal clearing. Because Run SB-CL-05 was one of the first tests performed in the LSTF, some of the data have zero offsets and unexplained sudden value changes.

The system pressure behavior is similar at all locations, so only the upstream-break pressure is compared (Fig. 3). There is excellent agreement until 23 s, when the rate of pressure decrease slows in the experiment. At 80 s the data and the calculation are in agreement again and remain in reasonable agreement throughout the remainder of the transient. The increase in rate of pressure decrease at 160 s in the data and 180 s in the calculation corresponds to loop seal clearing. The liquid temperature upstream of the break (Fig. 4) is in reasonable agreement with the data and indicates that there is liquid with significant subcooling at the break until 50 s. The mass flow rates out of the break are compared in Fig. 5 and the integral mass losses from the system are compared in Fig. 6. There is reasonable agreement between the data and the calculation. During the first 50 s the calculated flow decreases in a similar manner as the pressure, whereas the data indicate a much smaller change in the flow. One of the measured flows (FRE590) is derived from the catch-tank level change and, therefore, may not be able to follow the rapid changes in flow at the beginning of the transient. The integrated mass loss comparison (Fig. 6) indicates that the calculated mass loss until the time of loop seal clearing is 10% less than in the data. The difference in the system pressure between 23 and 80 s can be explained by the different steam generator secondary and upper head behavior in the calculation. The pressure in the secondary of the loop B steam generator is shown in Fig. 7. The TRAC input modeled the relief valve action based on information about set points and valve response; but, as the pressure comparison shows, there is insufficient agreement between the calculation and the data, which may be attributed to an inadequate

characterization of the value. The loop A steam generator behaved in a similar manner. The periods of high secondary pressure are evident in the primary pressure (Fig. 3). These high secondary pressures are not sufficient to explain the primary pressure between 23 and 80 s. The draining of the upper head occurred later and at a lower pressure in the calculation because the initial temperature of the fluid in the upper head was too low and had insufficient agreement with the data. This temperature (Fig. 8) is a function of the downcomer to upper head leakage flow and heat transfer between the upper plenum and upper head. In the TRAC model, all the core bypass was assumed to be between the downcomer and the upper head. The special leakage tests to determine core bypass did not identify the specific leakage paths. Apparently much of the leakage was between the downcomer and the upper plenum because the measured initial upper head fluid temperature is closer to the core-exit temperature than the core-inlet temperature. The calculated upper head fluid temperature is equal to the core-inlet temperature. The earlier draining of the upper head in the experiment is shown in Fig. 9, which compares the upper head differential pressure. The data have a zero offset of 2 kPa and unusual initial behavior, but indicate that draining of the upper head begins earlier in the experiment. As will be shown later, the calculation drains the top of the steam generator tubes instead of the upper head. These disagreements in pressure and draining are ended before the core uncover and, therefore, have no effect on the remainder of the calculation. Figure 10 shows the excellent agreement between the calculated and measured emptying of the pressurizer, with an apparent offset in the measurement.

The next series of plots shows the behavior in the vessel. The calculated downcomer liquid volume fraction is shown in Fig. 11. The measured and calculated downcomer differential pressures are compared in Fig. 12. The agreement is considered to be reasonable. In the calculation, the downcomer remained full until the loop seal cleared, but in the experiment the downcomer drained down to the nozzle level by 100 s and that remained constant until after the loop seal cleared. The voiding of the upper head in the experiment may have filled the top of the downcomer with vapor through the upper head leakage holes. The calculated core liquid volume fractions are shown in Fig. 13. The reasonable agreement between the calculated and measured core differential pressures is shown in Fig. 14. Initially, there is excellent agreement as the flow first increases and then decreases, but as core voiding begins the measured differential pressure decreases more rapidly than in the calculation. The core level is depressed until the loop seal clears and then the core is refilled rapidly. The slower calculated mass loss at the break explains the slower core level depression in the calculation and the later refill as the loop seal clears.

The temperature response of the heater rods is shown in the next series of plots. The maximum calculated temperature at any location on any of the heater rods is shown in Fig. 15. The temperature follows saturation except for the short period between 140 and 180 s when the core uncovers and some rod locations heat up until the core refills and the rods are again cooled. One rod from each of the three power zones was chosen as a comparison point for the measured and calculated temperatures at six axial locations. Figures 16, 17, and 18 show the temperatures for the intermediate, high, and low power zones respectively. There is moderate agreement between the data and the calculation. The calculated rod heatup is delayed but the temperature increase is correct. Inspection of all the measured rod temperatures shows considerable variation from rod to rod within the core at any given elevation so that the

agreement between the calculation and the data shown in Fig. 16 through 18 is typical of the average.

The steam generator differential pressure measurement is divided into upflow and downflow sides of the tubes. The differential pressure instruments are connected to show a positive reading when the tubes are full of liquid. This method of instrumentation means that flow losses are added to the liquid head on the upflow side and subtracted on the downflow side. The comparison of the measured to calculated differential pressure for steam generator A upflow and downflow sides are shown in Figs. 19 and 20 respectively. After the initial deflections caused by the increase in the flow, both sides show that the calculation begins draining the tubes earlier than the data, showing minimal agreement between TRAC and the data. As discussed above, this is due to the lower initial temperature of the fluid in the upper head. After 80 s, the agreement improves to moderate. Comparison of Fig. 19 to Fig. 20 shows that the downflow side drains at 100 s, while on the upflow side some liquid is held up and maintains a differential pressure of up to 30 kPa until the loop seal clears. The differential pressures for the loop B steam generator are similar to loop A. The upflow and downflow sides are shown on the same plot separately for the data (Fig. 21) and the calculation (Fig. 22). Both of these plots clearly show the liquid holdup between 80 and 180 s. The comparison of Fig. 21 to Fig. 22 shows reasonable agreement except between 25 and 80 s, when the agreement is minimal as a result of the early draining in the calculation. The pressure difference between the upflow and downflow sides is up to 30 kPa.

The loop seal differential pressure also is split in two sections. The downflow section connects the steam generator outlet to the bottom of the loop seal. The upflow section connects the bottom of the loop seal to the inlet of the pump. As in the case of the steam-generator differential pressure, the loop seal differential pressure is composed of a liquid head and flow loss. The upflow and downflow sides differential pressures for loop A are shown in Figs. 23 and 24, respectively. The downflow side (Fig. 23) begins clearing at the same time in both the data and the calculation, but the clearing proceeds more slowly in the calculation than the experiment. The upflow side (Fig. 24) clears after the downflow side. The agreement in Figs. 23 and 24 is considered reasonable.

VI. REVIEW OF S-UT-8 RESULTS

The test and TRAC analysis results of S-UT-8⁷ (a Semiscale run) are briefly reviewed here to compare with the results of SB-CL-05 for study of similar phenomena at two different scales. In the S-UT-8 experiment there were two core uncoveries (Fig. 25). The early core uncover is of interest because it is caused by liquid holdup in the steam generator. There is reasonable agreement between the measured and predicted core levels. The calculated time of the first core level depression is delayed by 25 s. The rod temperature response at the 208-cm elevation is shown in Fig. 26. There was limited heatup during the early core liquid level depression. The predicted temperature increase was in reasonable agreement with the data even though the first heatup was delayed 50 s. The delay in core depression in the calculation may have been caused by the slower mass loss out of the break (Fig. 27). The clearing of the loop seal occurs at the same mass loss. The collapsed liquid level in the upflow and downflow sides of the intact-loop steam generator is shown in Fig. 28. The liquid holdup or delay in draining on the upflow side of the steam generator is shown in both the data and

the calculation. The level difference was 2.5 to 3.0 m. The calculated clearing of the upflow and downflow sides of the steam generator was delayed 50 s compared to the data.

Comparison of the behavior of S-UT-8 to SB-CL-05 shows very similar phenomena in both tests with some timing differences when this phenomenon occurred. The core uncover was almost complete in both tests (Figs. 14 and 25). In response to the core uncover, the cladding temperature increased by 100 K (Figs. 17 and 26). The loop seal clearing and core recovery occurred at the time that the primary inventory was reduced to 30% of the initial inventory (Figs. 6 and 27). The liquid holdup or delay in draining on the upflow side of the steam generator caused a pressure imbalance that was equivalent to 2.5 to 3.0 m of water (Figs. 23, 24, and 28). The similarity of phenomena in these two facilities builds confidence that these results can be expected to occur in a PWR. The Semiscale facility is a 1/1650 scale model and the LSTF is a 1/48 scale model. Scaling from Semiscale to the LSTF is an increase in scale of 35 times so that this is almost halfway to full scale. One of the important phenomena is the steam generator liquid holdup. Similar holdup has now been observed in the 6 tubes of Semiscale and the 141 tubes of LSTF. It is now more believable that holdup may occur in a full-scale steam generator with 3000 or more tubes. The ability of the TRAC code to calculate the phenomenon equally well in the two different scaled experiments confirms the scalability of the many models in the code that are important in calculating this small break.

VII. CONCLUSIONS

Experiment SB-CL-05 in the LSTF showed that, for a 5% cold-leg break, a core uncover occurred and that during this uncover the rod cladding temperatures increased by 100 K. The core uncover occurred when the loop seal was full and a pressure imbalance between the upflow and downflow sides of the steam generator was the equivalent of 2.5 to 3.0 m of liquid. When the loop seal cleared, the core recovered and the rods cooled.

The TRAC calculation of this experiment was in reasonable agreement with the data. TRAC predicted all the major trends and phenomena but was 20 s late in the timing. The predicted liquid holdup on the upflow side of the steam generator was in agreement with the data. The manometric pressure imbalance from the liquid holdup caused the core to uncover and the rod cladding temperature to increase. The clearing of the loop seal relieved the pressure imbalance and allowed the core to recover and cool.

The similarity of Semiscale Run S-UT-8 to LSTF Run SB-CL-05 confirms the scaling of the small-break phenomena observed in these experiments. The scale change from Semiscale to LSTF is 35 times, which is close to the 48-times scale change between LSTF and full scale. The reasonable agreement between the TRAC code calculation and the experimental data of these two tests confirms the scalability and accuracy of the small-break modeling in the code.

REFERENCES

1. Safety Code Analysis Group. "TRAC-PF1/MOD1: An Advanced Best-Estimate Computer Program for Pressurized Water Reactor Thermal-Hydraulic Analysis." Los Alamos National Laboratory report LA-10157-MS (NUREG/CR-3858)(July 1986).
2. Personal Communication. K. Tasaka.
3. M. T. Leonard. "Vessel Coolant Mass Depletion during a Small Break LOCA." EG&G report EGG-SEMI-6010 (September 1982).
4. G. G. Loomis and J. E. Streit. "Quick Look Report for Semiscale Mod-2c Experiments S-LH-1 and S-LH-2." EG&G report EGG-SEMI-6884 (1985).
5. ROSA-IV Group. "ROSA-IV Large Scale Test Facility (LSTF) System Description." JAERI report JAERI-M 84-237 (1985).
6. F. Motley and R. Schultz. "A TRAC model of the Large-Scale Test Facility." Los Alamos National Laboratory report (to be published).
7. R. K. Fujita. "TRAC-PF1/MOD1 POSTTEST ANALYSIS OF SEMISCALE SMALL-BREAK TEST S-UT-8." Los Alamos National Laboratory report, LA-UR-85-961 (1985).

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TABLE I
INITIAL CONDITIONS FOR RUN SB-CL-05

Parameter	Test Data	TRAC Results
Pressurizer pressure (MPa)	15.6	15.58
Loop A hot-leg fluid temperature (K)	599	601.8
Loop B hot-leg fluid temperature (K)	599	601.8
Loop A cold-leg fluid temperature (K)	565	565.0
Loop B cold-leg fluid temperature (K)	564	565.0
Core power (MW)	10	10.04
Core inlet flow (kg/s)	N/A	45.32
Loops A and B flow (kg/s)	49.25 ^a	47.43
Bypass (%)	N/A	4.45
Loop A flow (kg/s)	24.6 ^a	23.57
Loop B flow (kg/s)	24.65 ^a	23.86
Pressurizer water level (m)	2.6	2.6
Loop A primary-coolant pump speed (rpm)	776 ^a	776.1
Loop B primary-coolant pump speed (rpm)	785 ^a	785.4
Primary-coolant flow-control valve	fully open	fully open
Steam-generator-secondary pressure (MPa)	7.3	7.393
Steam-generator-secondary liquid level (m)	10.3	10.3
Steam-generator feedwater temperature (K)	495	495
Steam-generator feedwater and steam flows (kg/s)	2.7	2.76

^aInformation retrieved from data tape.

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TABLE II
CHRONOLOGY OF EVENTS FOR RUN SB-CL-05

Event	Test Data (s)	TRAC Results (s)
Break	0	0
Reactor trip	12	N/A
Main steam-line valve closure	15	15
Safety injection signal	17	N/A
Steam-generator feedwater termination	18	17
High-pressure charging injection	31	37
High-pressure safety injection	34	37
Auxiliary feedwater initiation	40	48
Power decrease	45	45
Core uncover	120-155	140-175
Loops A and B loop-seal clearing	~140	~160
Primary and secondary pressure reversal	~180	~180
Reactor coolant pumps' termination	266	266
Accumulator flow begins	416	416

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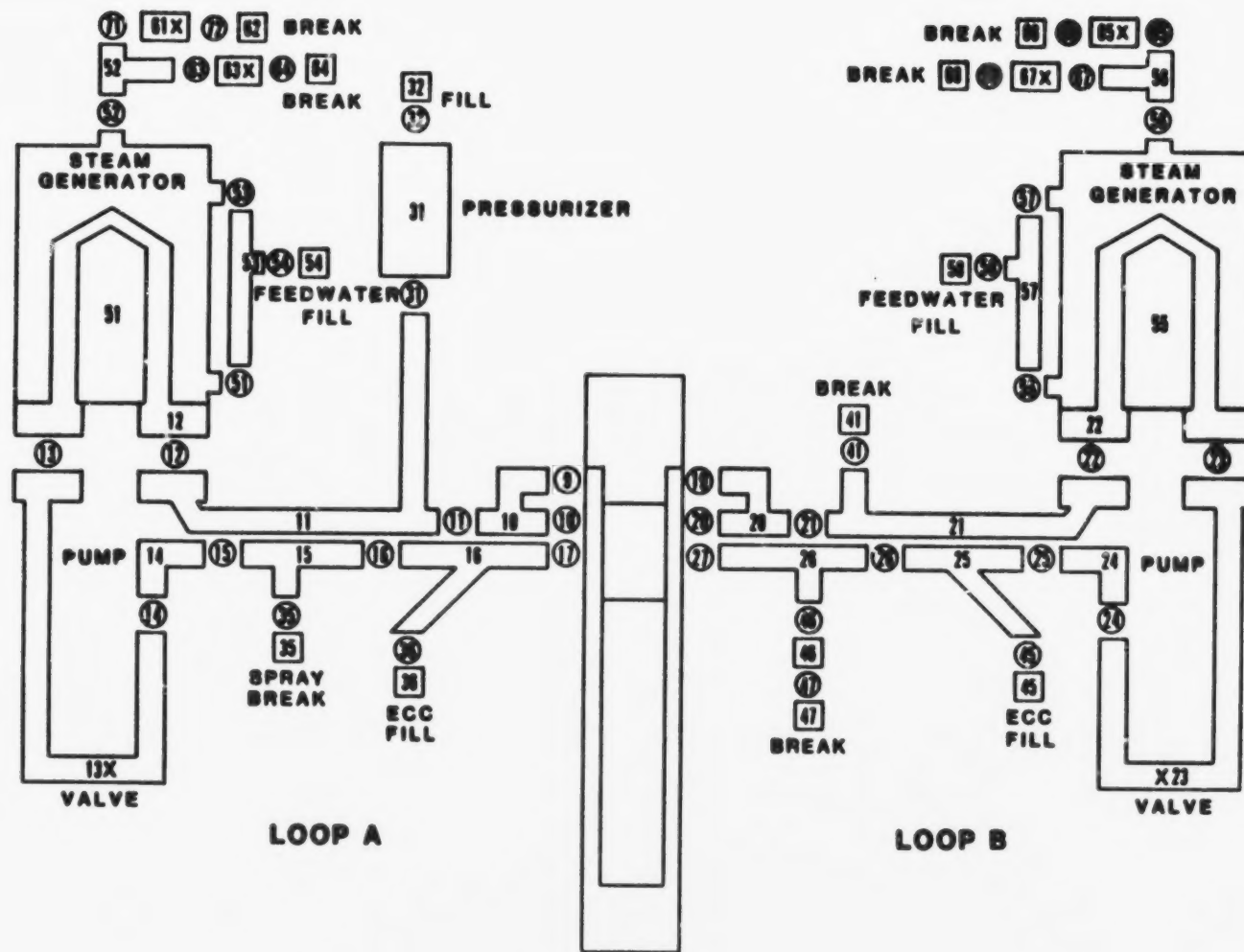


Fig. 1.
TRAC input description of LSTF.

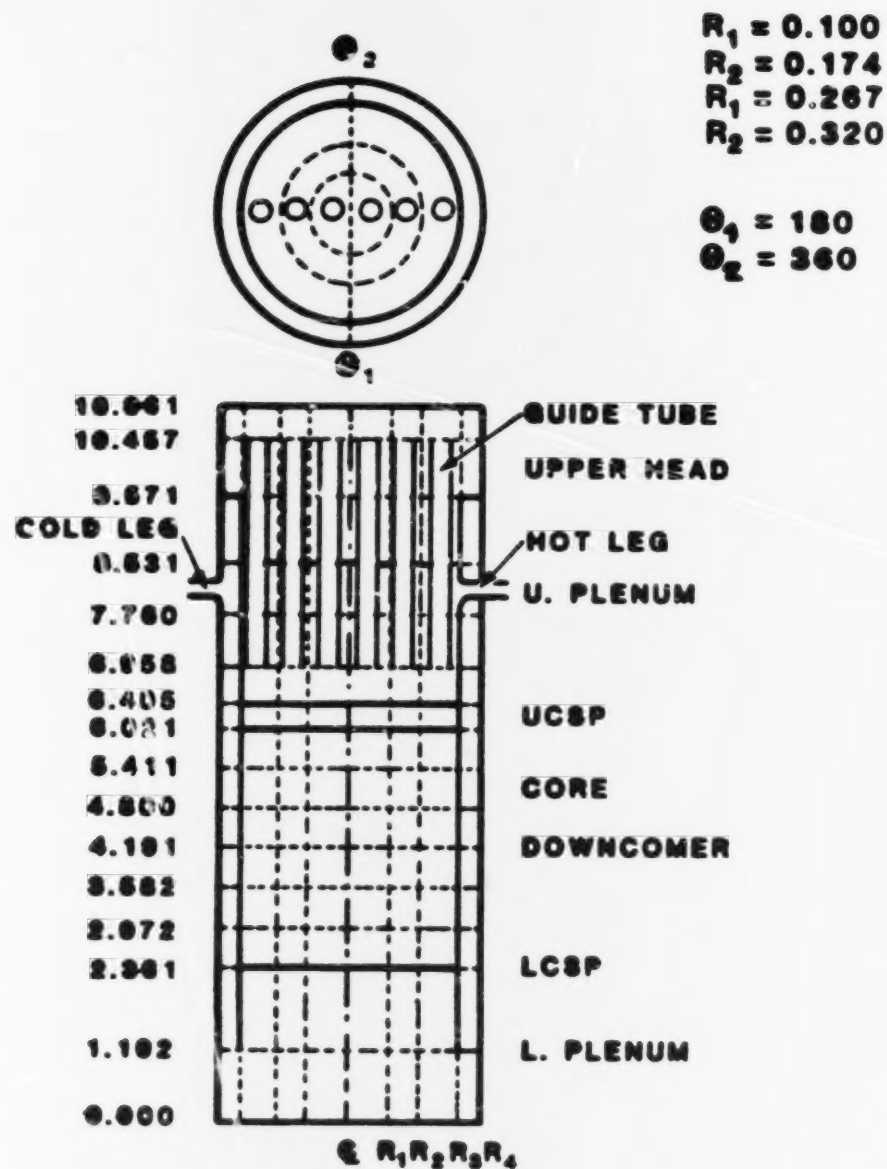


Fig. 2.
Detailed TRAC model of LSTF vessel.

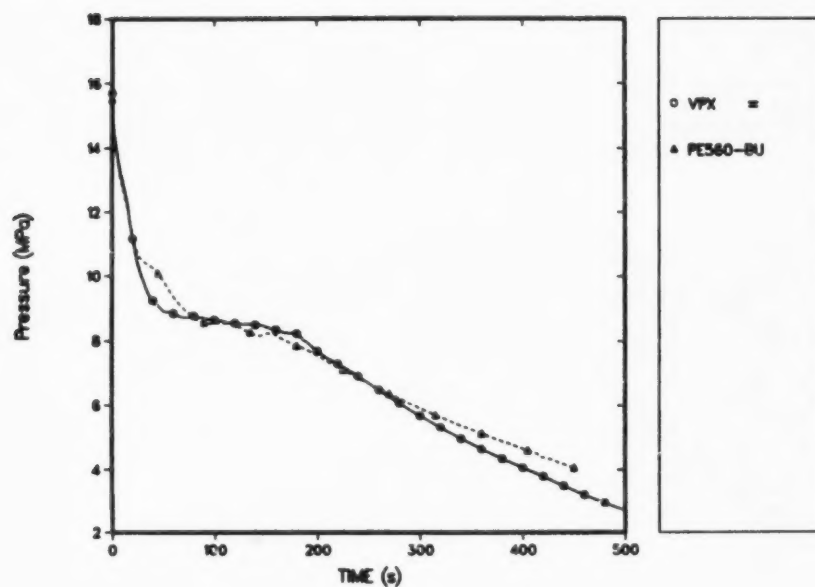


Fig. 3.

Comparison of TRAC-calculated (solid line) and measured (dashed line) upstream break pressure.

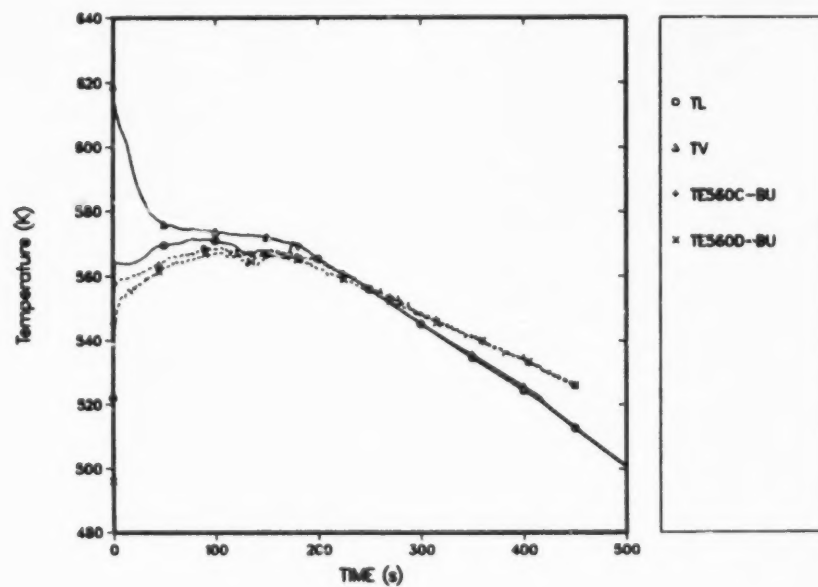


Fig. 4.

Comparison of TRAC-calculated (solid line) and measured (dashed line) upstream break temperature.

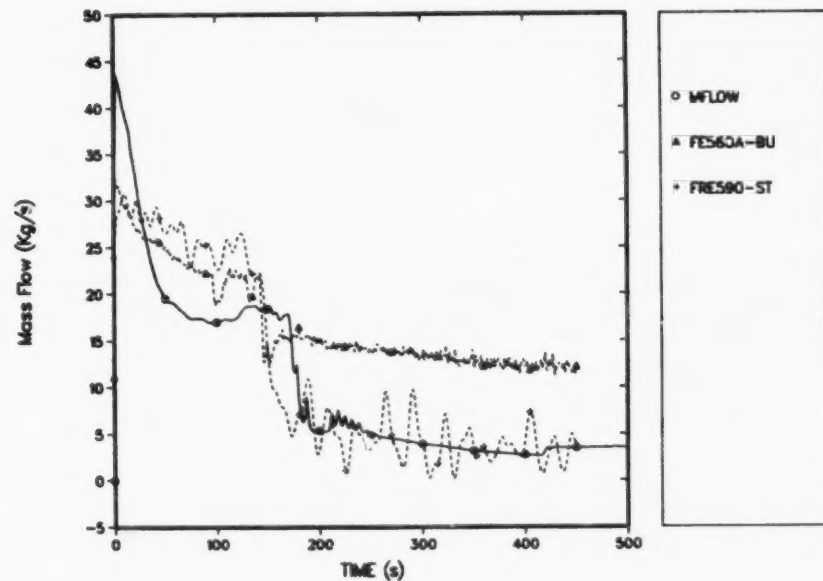


Fig. 5.

Comparison of TRAC-calculated (solid line) and measured (dashed line) break mass flow.

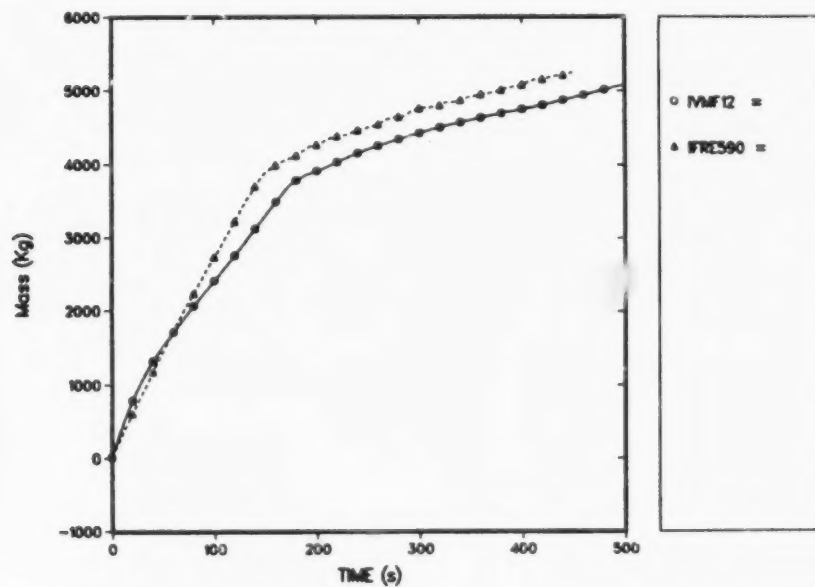


Fig. 6.

Comparison of TRAC-calculated (solid line) and measured (dashed line) mass loss out of break.

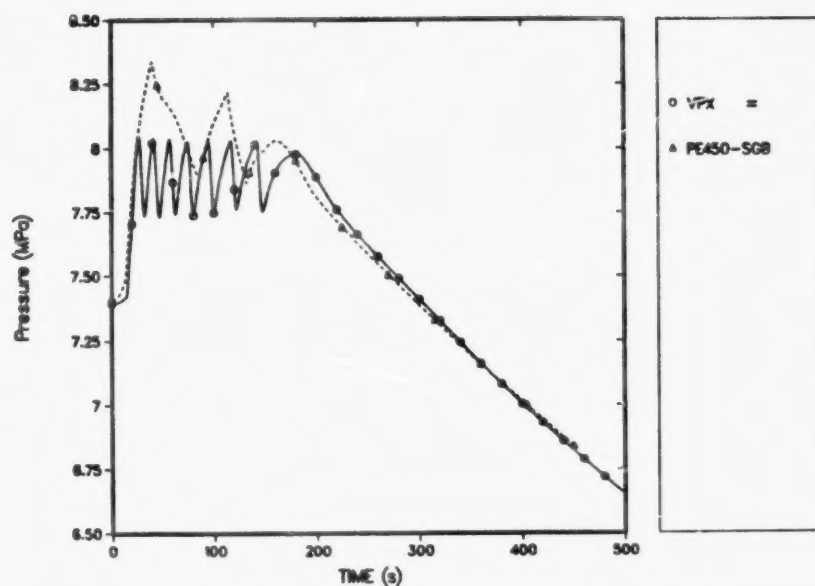


Fig. 7.
Comparison of TRAC-calculated (solid line) and measured (dashed line) steam generator secondary pressure.

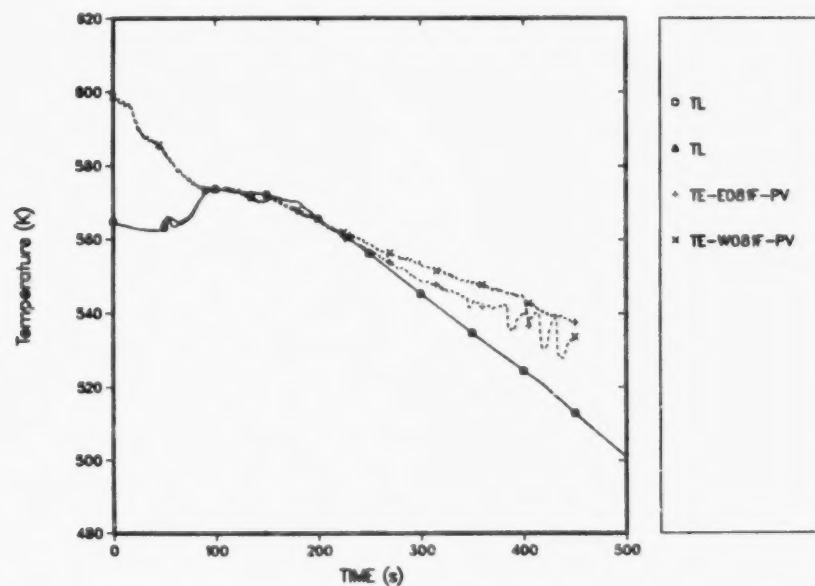


Fig. 8.
Comparison of TRAC-calculated (solid line) and measured (dashed line) upper head temperatures.

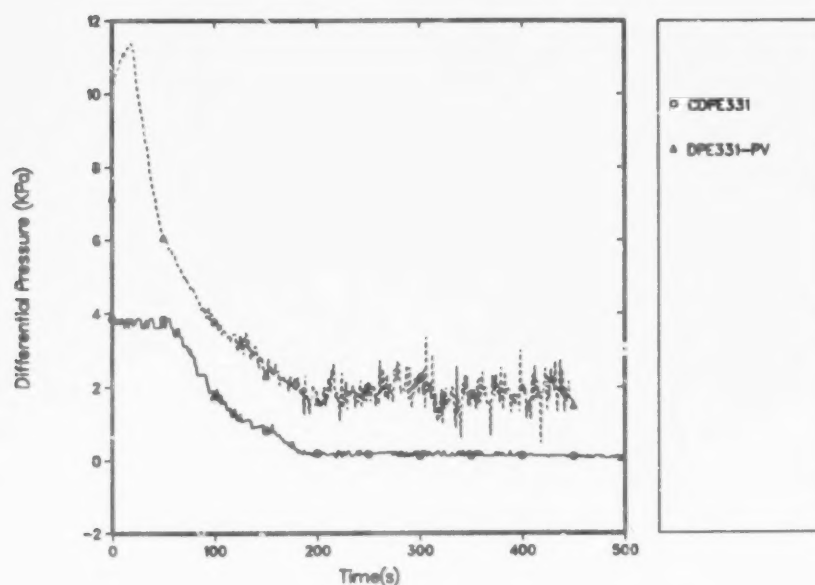


Fig. 9.

Comparison of TRAC-calculated (solid line) and measured (dashed line) upper head differential pressure.

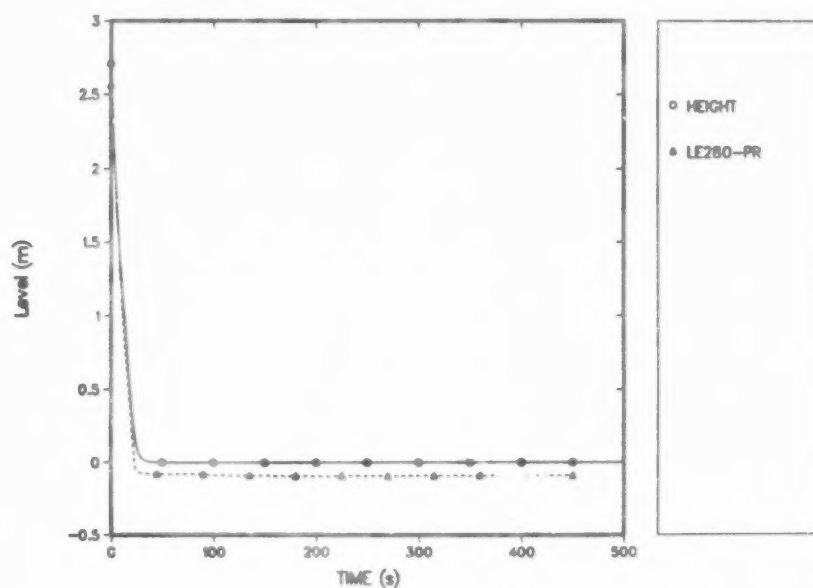


Fig. 10.

Comparison of TRAC-calculated (solid line) and measured (dashed line) pressurizer level.

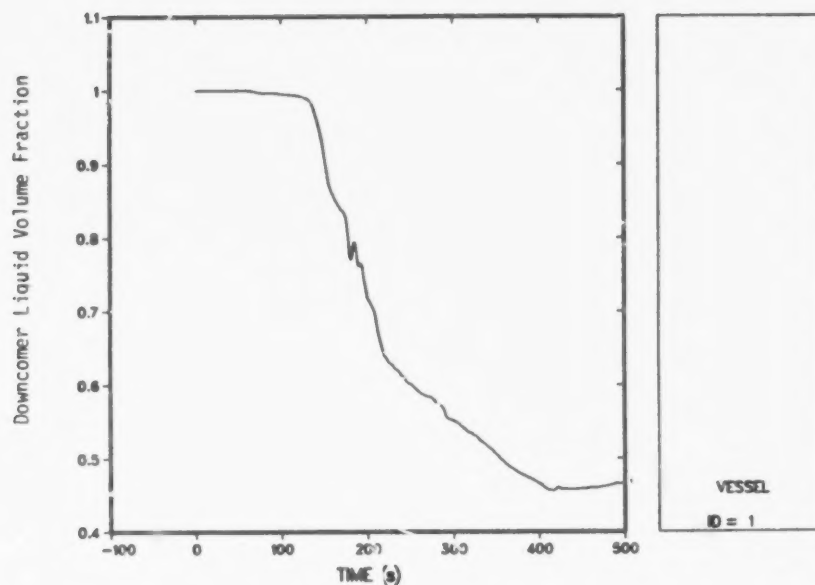


Fig. 11.
Calculated downcomer liquid volume fraction.

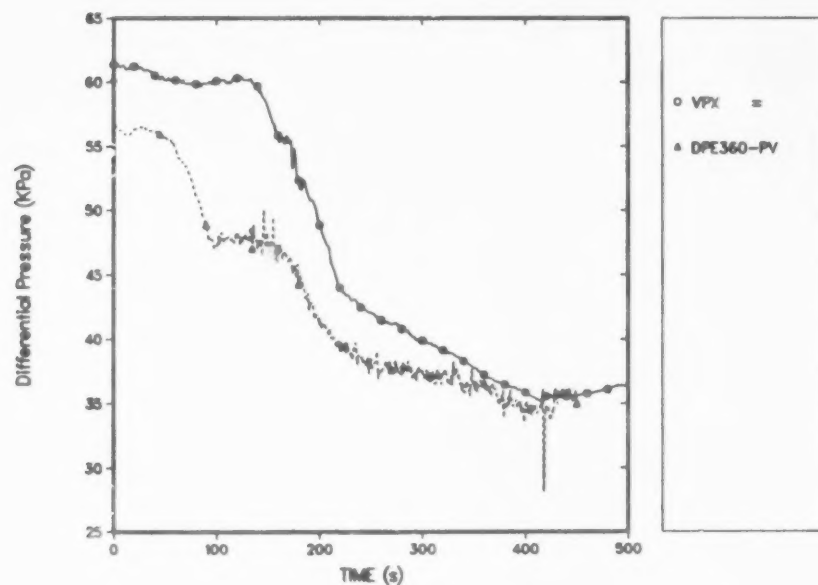


Fig. 12.
Comparison of TRAC-calculated (solid line) and measured (dashed line)
downcomer differential pressure.

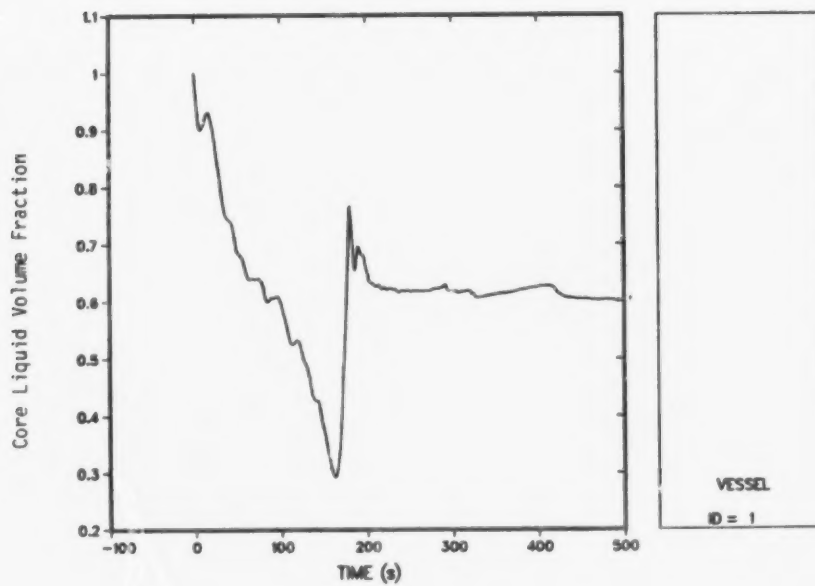


Fig. 13.
Calculated core liquid volume fraction.

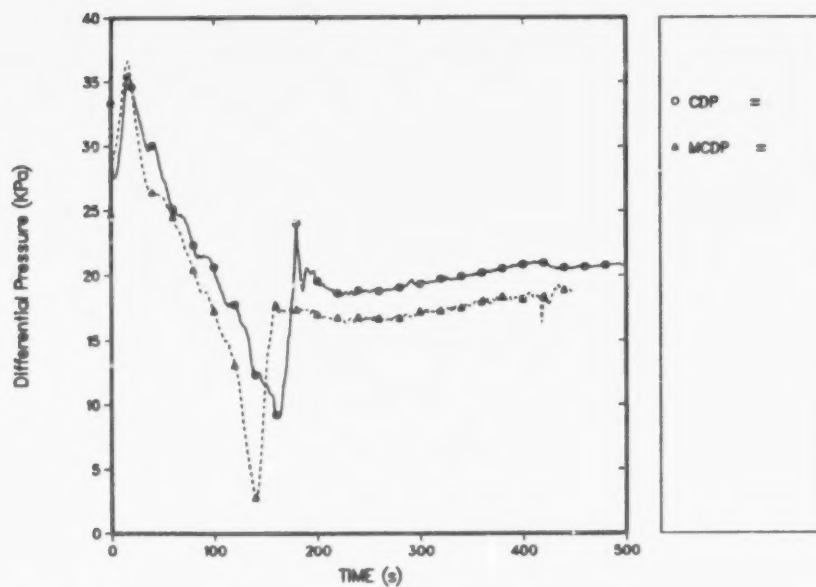


Fig. 14.
Comparison of TRAC-calculated (solid line) and measured (dashed line)
core differential pressure.

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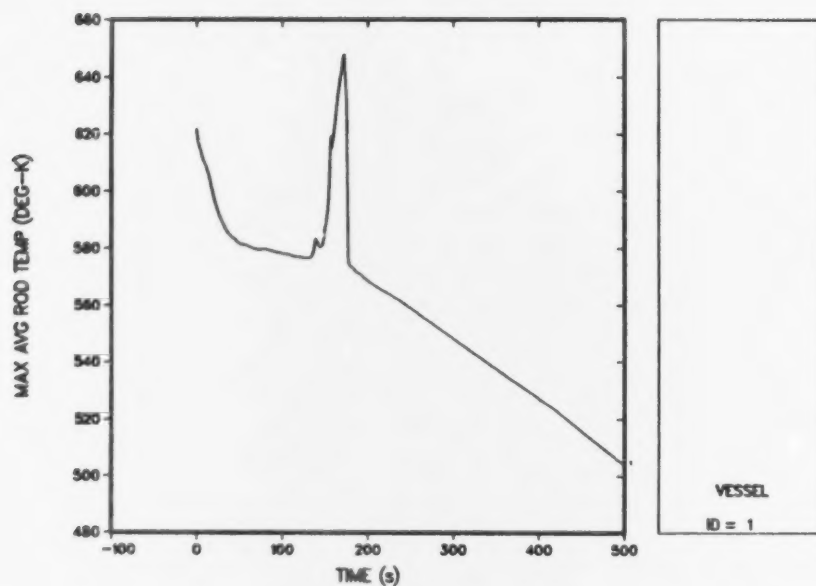


Fig. 15.
Calculated maximum cladding temperature of an average rod.

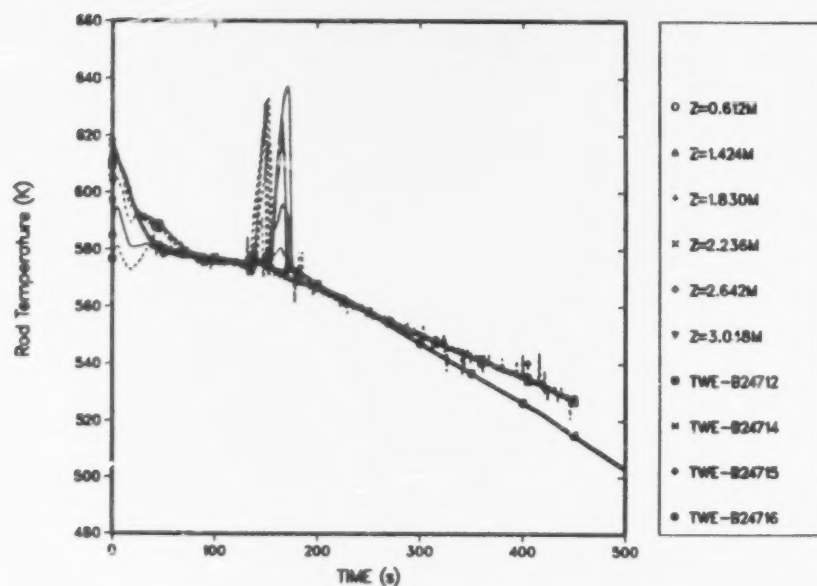


Fig. 16.
Comparison of TRAC-calculated (solid line) and measured (dashed line)
rod surface temperatures from bundle 24.

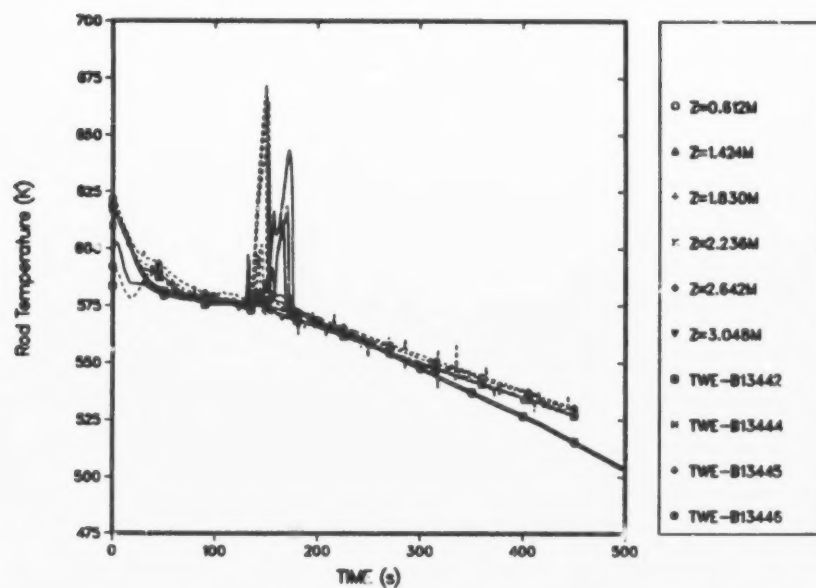


Fig. 17.
Comparison of TRAC-calculated (solid line) and measured (dashed line) rod-surface temperatures from bundle 13.

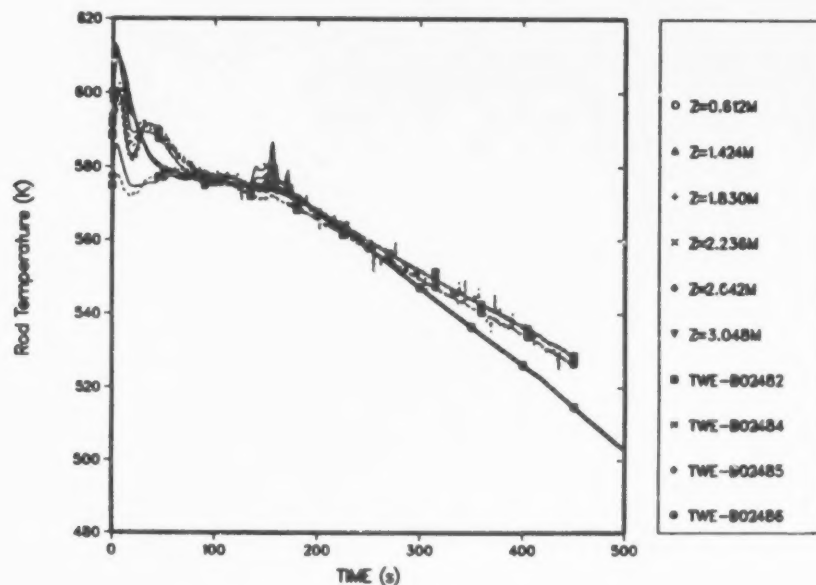


Fig. 18.
Comparison of TRAC-calculated (solid line) and measured (dashed line) rod-surface temperature from bundle 2.

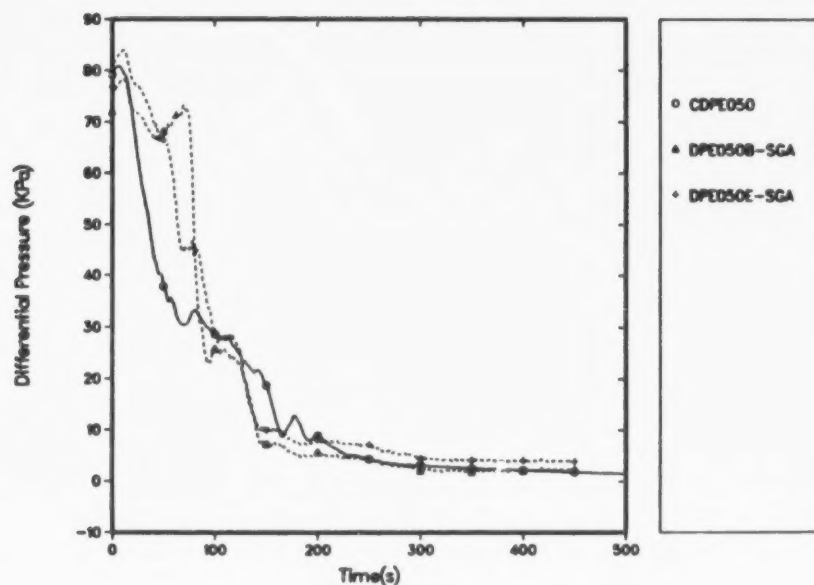


Fig. 19.

Comparison of TRAC-calculated (solid line) and measured (dashed line) Loop A steam generator upflow side differential pressure.

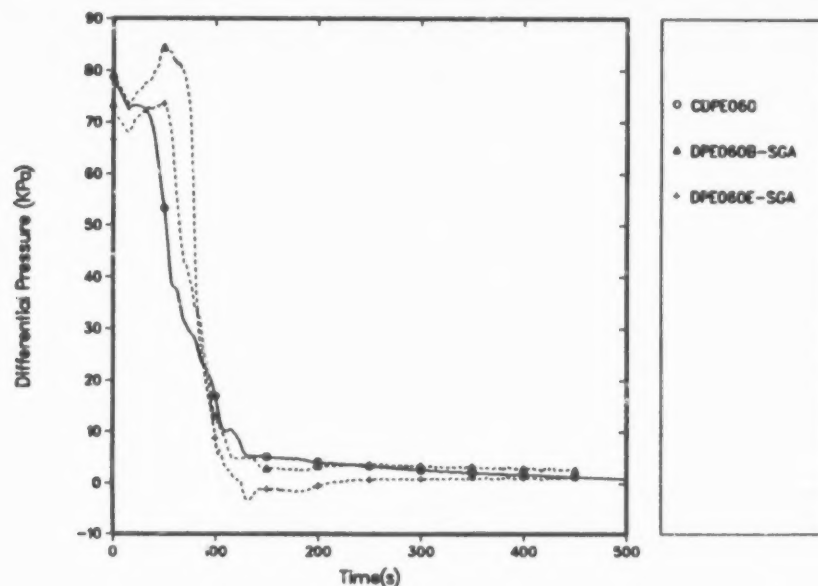


Fig. 20.

Comparison of TRAC-calculated (solid line) and measured (dashed line) Loop A steam generator downflow side differential pressure.

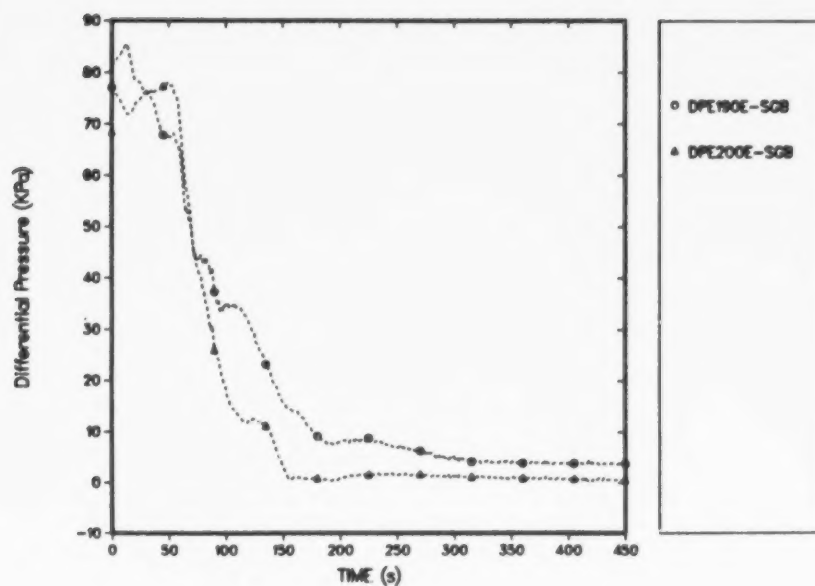


Fig. 21.
Measured liquid holdup in steam generator B.

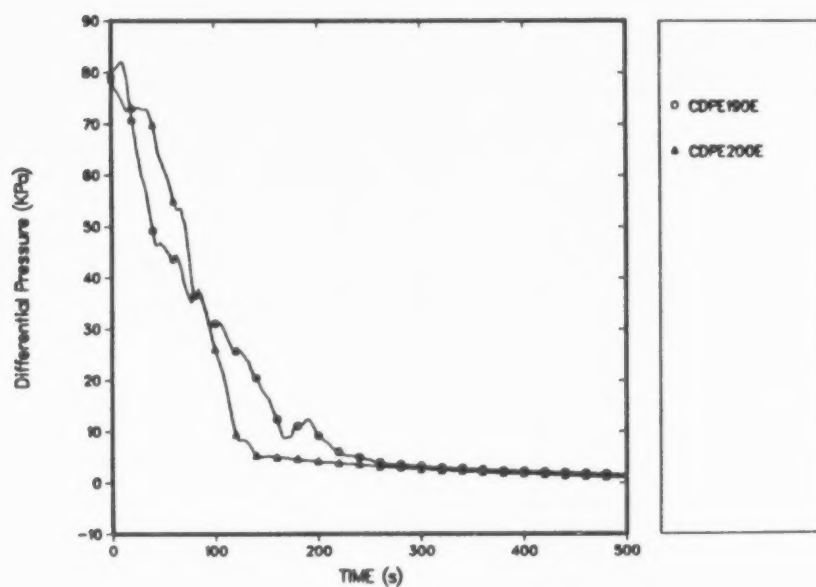


Fig. 22.
TRAC-calculated liquid holdup in steam generator B.

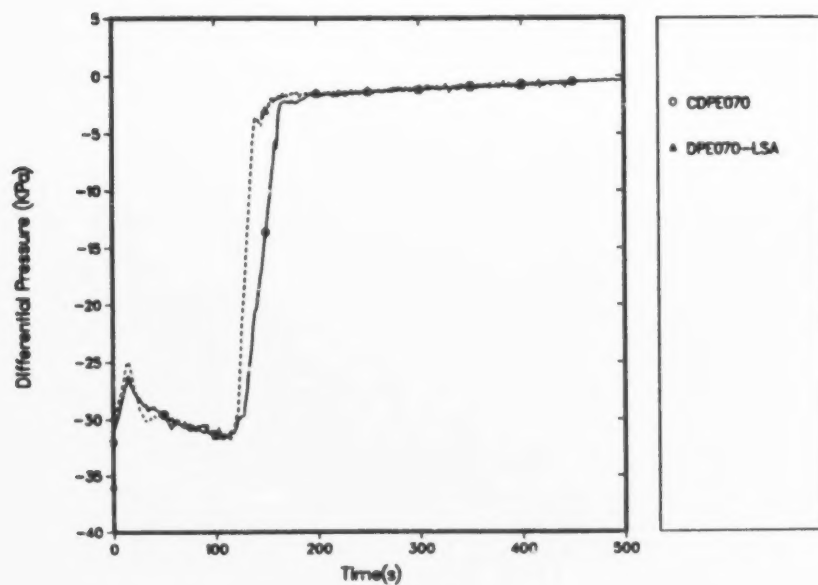


Fig. 23.

Comparison of TRAC-calculated (solid line) and measured (dashed line) Loop A loop seal downflow side differential pressure.

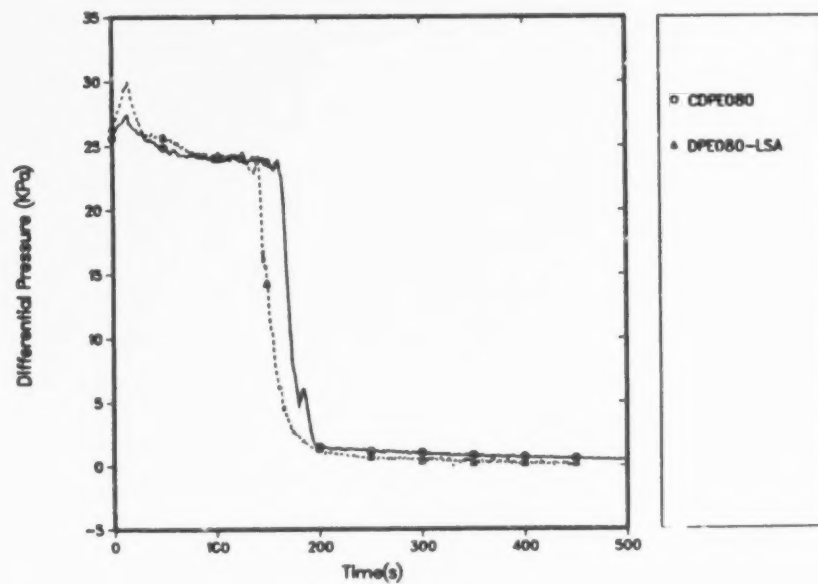


Fig. 24.

Comparison of TRAC-calculated (solid line) and measured (dashed line) Loop A loop seal upflow side differential pressure.

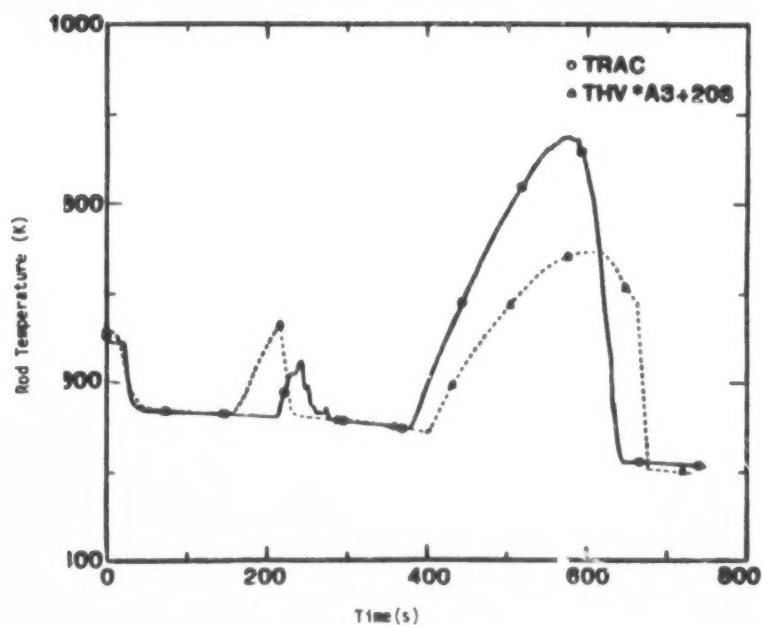


Fig. 25.

Comparison of TRAC-calculated (solid line) and measured (dashed line) collapsed liquid level in the core for Run S-UT-8.

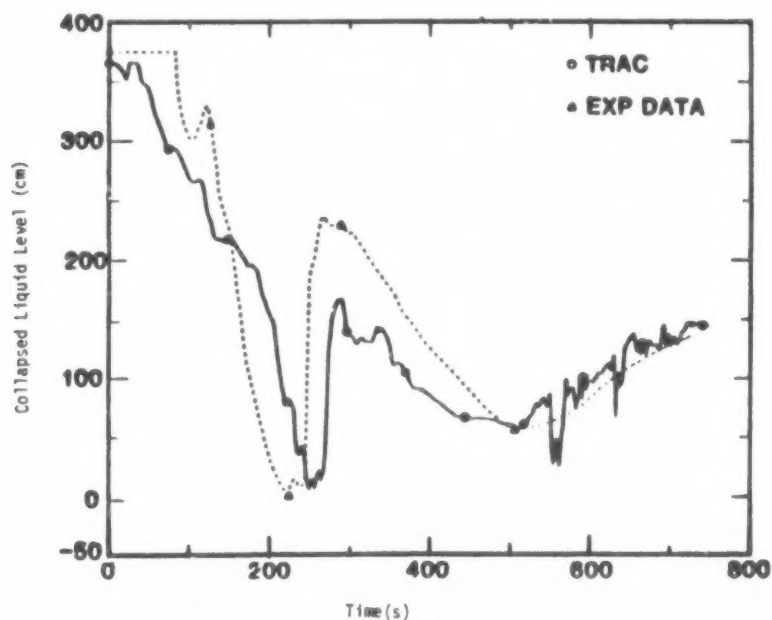


Fig. 26.

Comparison of TRAC-calculated (solid line) and measured (dashed line) rod-surface temperature of rod A3 at the 208-cm elevation for Run S-UT-8.

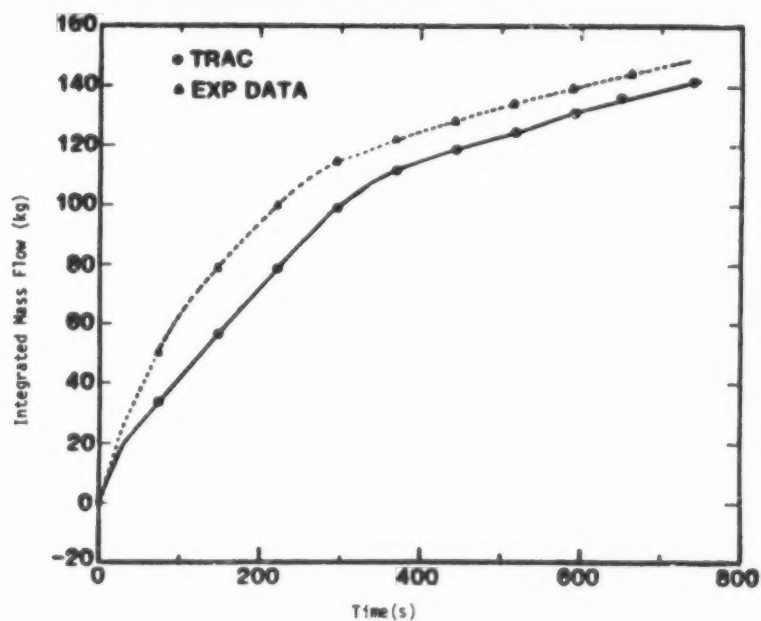


Fig. 27.

Comparison of TRAC-calculated (solid line) and measured (dashed line) mass loss out of break for Run S-UT-8.

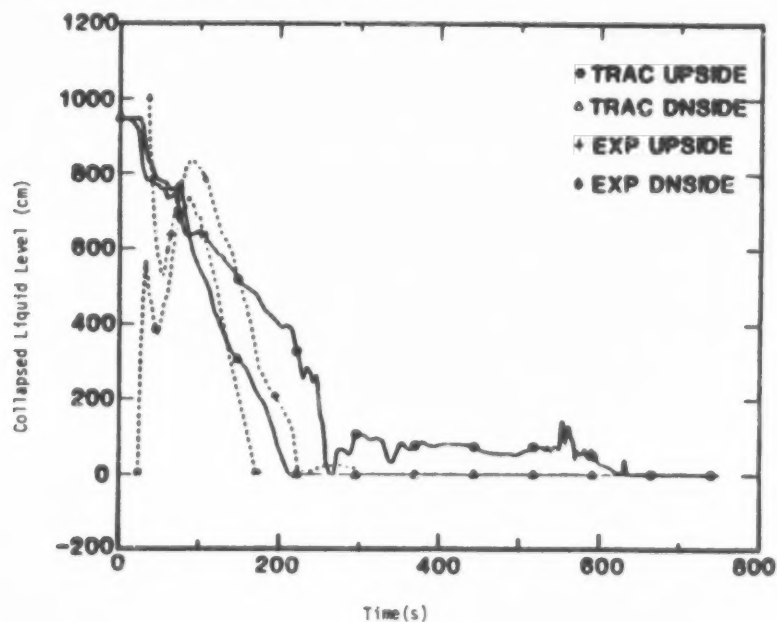


Fig. 28.

Comparison of TRAC-calculated (solid line) and measured (dashed line) liquid holdup in the intact-loop steam generators for Run S-UT-8.

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Management of Emergency Feedwater During Natural
Circulation Cooldown Following a Loss of
Offsite Power Scenario

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Abstract

As the result of calculations performed at Florida Power Corporation during 1986, a better appreciation has been obtained of the flexibility and the limitations associated with managing emergency feedwater (EFW) inventory while providing emergency cooling to pressurized water reactors. This insight was gained while investigating the EFW inventory requirement following a loss of offsite power scenario. In general, an operator may (1) use a wide range of cooldown rates and (2) wait several hours before initiating a cooldown without significantly varying the total amount of EFW required to complete a cooldown. Although the studies performed were related to the specific conditions of the Crystal River 3 (CR-3) Nuclear Generating Station, we believe that the information gained is generically applicable to pressurized water reactors under conditions where the atmospheric dump valves (ADVs) limit the time to cooldown.

Summary

1. The operator may choose a cooldown rate (CDR) between two limits: the upper limit of CDR is the permissible rate to avoid forming a steam bubble in the reactor vessel head; the lower limit is approximately the linear slope of the line connecting the initial operating hot leg temperature at the beginning of cooldown and the Decay Heat Removal (DHR) System cut-in temperature at the end of the minimum cooldown time. The minimum cooldown time is mainly determined by the available steam dump capacity at the DHR System cut-in temperature.
2. The choice of cooldown rates, between the limits above, to the minimum cooldown time does not significantly affect the amount of EFW required to cooldown.
3. If the operator chooses to wait at hot standby, prior to cooling down, the least amount of EFW is required to cooldown.
4. Relatively inexpensive options may be available to reduce both the cooldown times and the EFW requirements to obtain the DHR cut-in conditions.

Discussion

In 1980, during a natural circulation cooldown of the St. Lucie-1 Nuclear Generating Station, an unexpected bubble of steam formed in the reactor vessel head. The explanation offered was a loss of subcooled margin in the head region, which had not cooled as quickly as other portions of the primary system. The NRC's Generic Letter 81-21 (Ref. 1) discussed the event and requested PWR utilities to determine the rate of cooldown which would be utilized during natural circulation to avoid the formation of a steam bubble in the head.

Generic Letter 81-21 also requested PWR utilities to confirm that EFW reserves were sufficient to support natural circulation cooldown to the DHR cut-in temperature. This request implied a rather direct connection between EFW inventory requirements and the rate of cooldown.

For a number of reasons, a wide range of different cooldown rates were proposed by utilities having similar Nuclear Steam Supply Systems designed by Babcock and Wilcox (B&W). The NRC suggested that the owners of B&W reactors, in a move toward greater uniformity, consider reviewing and applying a methodology submitted by General Public Utilities Nuclear (GPUN) in 1984 with the assistance of Energy, Inc. (EI). The methodology of GPUN/EI was later adopted by Florida Power Corporation and several other B&W reactor owners during 1985. The not-to-exceed cooldown rate in this methodology was 50 degrees Fahrenheit (F) per hour (H) under natural circulation cooldown conditions (Ref. 2).

The Florida Power Corporation authors of this paper became interested in 1) whether an optimum rate of cooldown existed which might conserve EFW inventory and 2) how long the operator might wait for restoration of AC power prior to beginning cooldown.

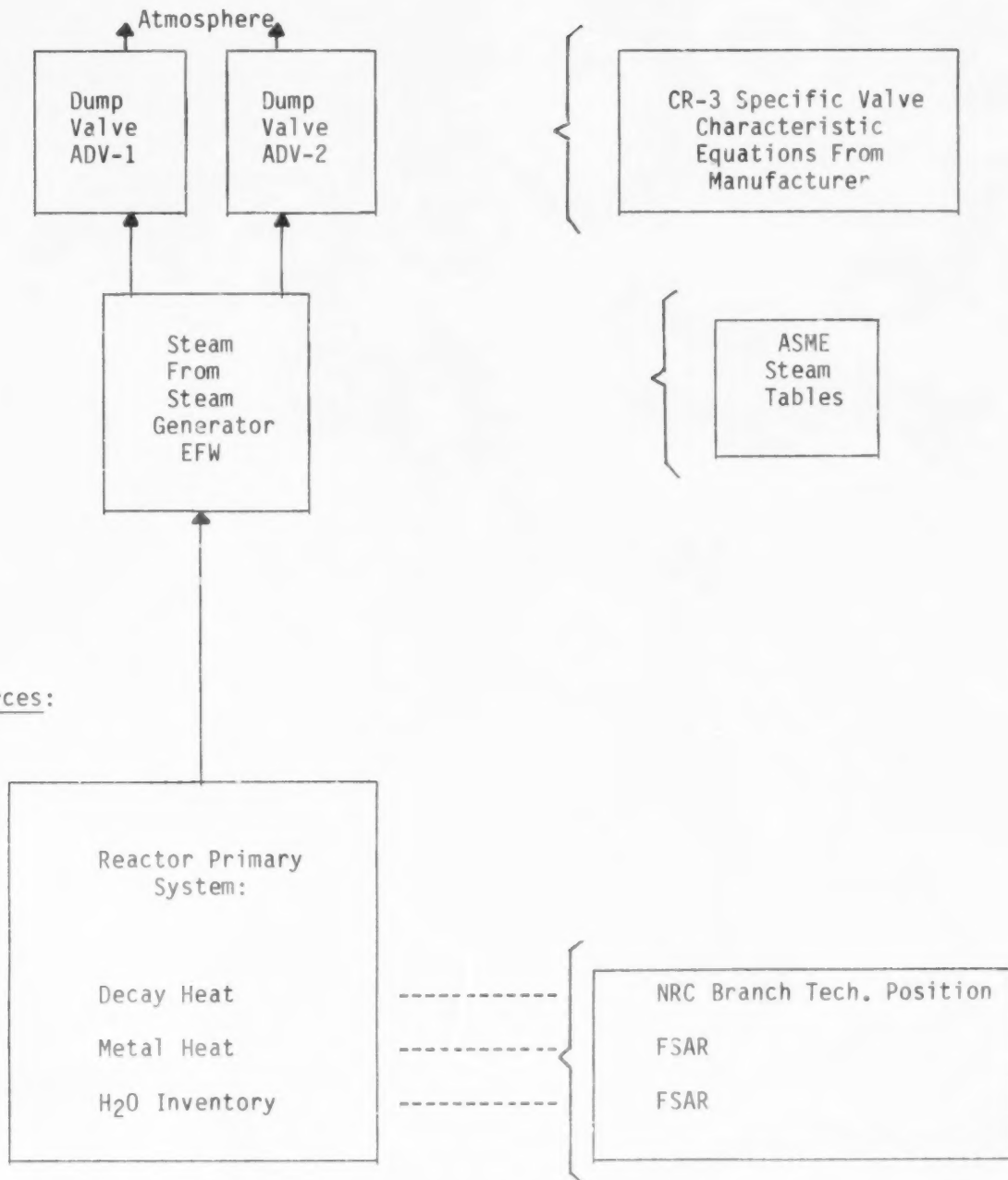
A computer code FEEDWATER INVENTORY, NATURAL CIRCULATION (FINC) was developed for performing these calculations in-house on an IBM-PC. The slide titled "Modeling of EFW Utilization" shows the FINC modeling of heat sources and sinks. The initial (FINC-1) results, presented in the published Transaction Report for this meeting, included a minimum cooldown time of 91 hours to 280F, a maximum possible wait of 77 hours, and a minimum cooldown rate of 5F/H. All of these paths reached 280F in approximately the same minimum time and consumed approximately the same amount of feedwater. Although the calculated values have changed for more refined models (FINC-2 and FINC-3), the initial explanation for these results remains the same: The amount of energy generated between the time of reactor shutdown and the minimum time to cooldown must be matched approximately by the energy removed in changing the EFW from liquid to steam. Therefore, approximately the same amount of EFW is required for all cooldown rates which lead to the minimum cooldown time. The FINC-3 model includes ambient heat losses to the containment and improved modeling of the ADVs and their associated steam exhaust (tail) pipes. For the Crystal River 3 nuclear plant, FINC-3 results are shown in the following slides.

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HEAT SOURCES AND SINKS

MATH MODELS

Sinks:



MODELING OF EFW UTILIZATION

Figure 1 shows three results:

Curve 1 is a plot of hot leg temperature vs. time assuming the cooldown is initiated immediately at 50F/H. During the linear part of the curve, the ADVs are not fully open. As the pressure decreases across the ADVs, the opening is increased to permit continued energy balance for removal of heat from nuclear decay, system metal, and system water. When the curve begins to bend at about 350F, the ADVs are fully open. As the pressure across the valve continues to decrease with decreasing temperature, the energy removal from the ADVs is sufficient to remove the decay heat, but not sufficient to continue cooldown at the same rate. The result is a much slower, almost asymptotic, approach to a hot leg temperature of 280F. When 280F is attained, the DHR System is initiated.

The time to DHR initiation is about 45 hours. All curves in Figure 1 assume the conservative decay heat used in the NRC Standard Review Plan (Ref. 3). Curve 1 is repeated in other figures as a reference case.

Curve 2 of Figure 1 indicates an approximately linear cooldown initiated immediately at 8.5F/H. This curve also required about 45 hours to cooldown.

Curve 3 of Figure 1 indicates the operator could wait about 29 hours on hot standby prior to cooling down at a rate of 50F/H. Time to DHR initiation under these conditions remains at 45 hours.

The principal message from Figure 1 is that a variety of cooldown rates greater than 8.5F/H is possible, and a variety of waiting times less than 29 hours is possible without exceeding the minimum time to cooldown.

Figure 2 shows the calculated requirements of EFW to support natural circulation cooldown for the same three cases of Figure 1. The message is that all three cases require approximately the same amount of EFW for the minimum cooldown time. Note that less EFW is required for case 3 than for cases 1 and 2. Case 3 requires less EFW since only decay heat is being removed for about 29 hours. During the 29-hour period, the heat lost from the system to the containment reduces the amount of EFW required to cooldown.

In Figure 3, curves 1, 2, and 3 show the effect of using more realistic decay heat than assumed in the Standard Review Plan. Curve 4 is the same as shown in Figure 1 with conservative decay heat. Note that the minimum cooldown time has moved from about 45 hours to about 34 hours. The maximum waiting time has moved from 29 hours to about 16 hours. In order to achieve the minimum cooldown time of 34 hours, the near-linear cooldown rate has increased to 11.5F/H. Thus, the degree of flexibility to the operator has reduced somewhat as the result of more realistic assumptions. The waiting time should be 16 hours or less, and the cooldown rate should be 11.5F/H or greater. Values depend on when cooldown is initiated.

Figure 4 shows the cumulative EFW requirements for the realistic cases 1, 2, and 3 to be about 270,000 gallons. In the conservative case, shown by curve 4, the requirement is about 340,000 gallons.

FIGURE 1. TEMPERATURE-TIME BOUNDARIES OF OPERATOR RESPONSE
ASSUMING CONSERVATIVE DECAY HEAT

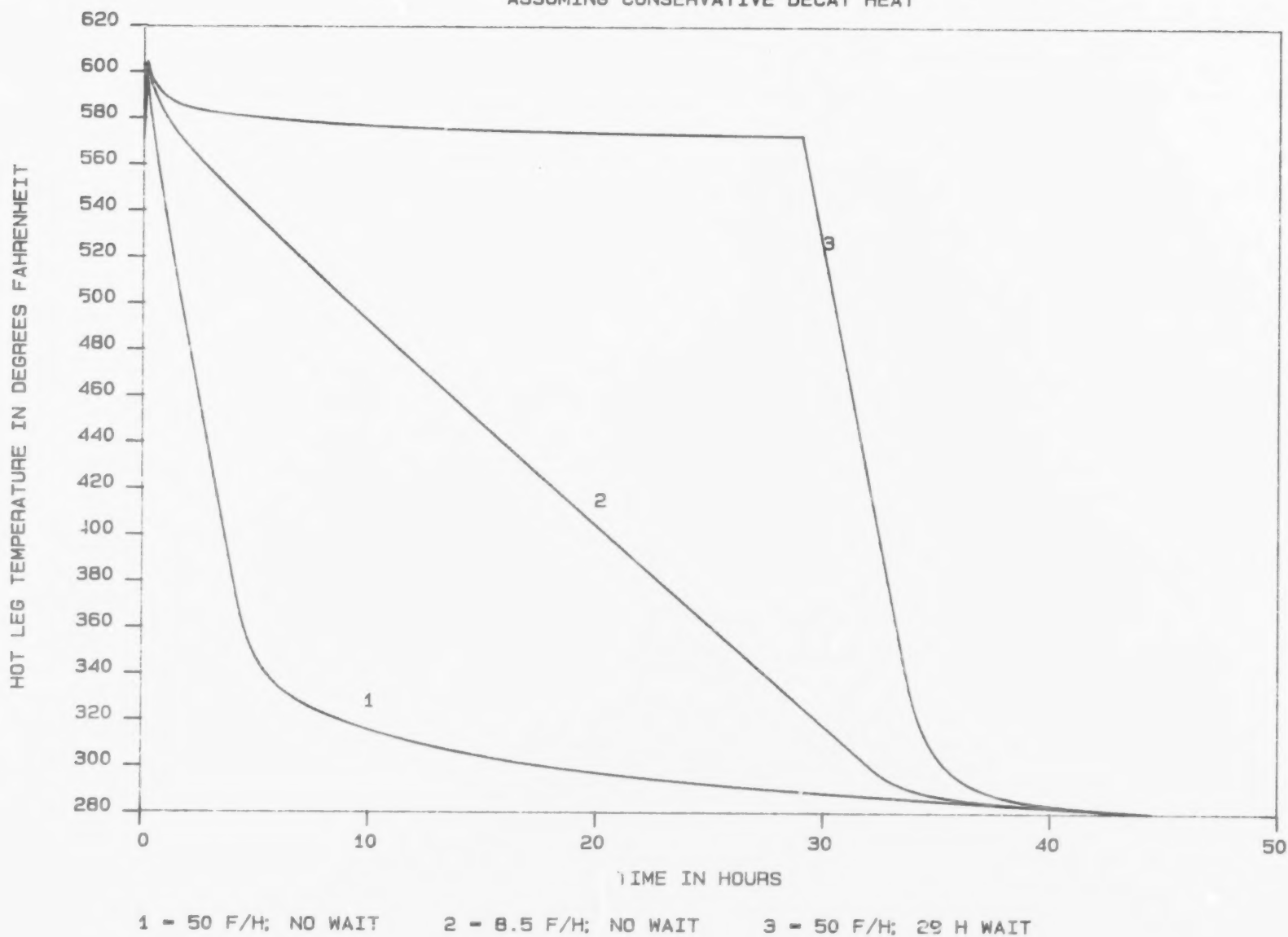
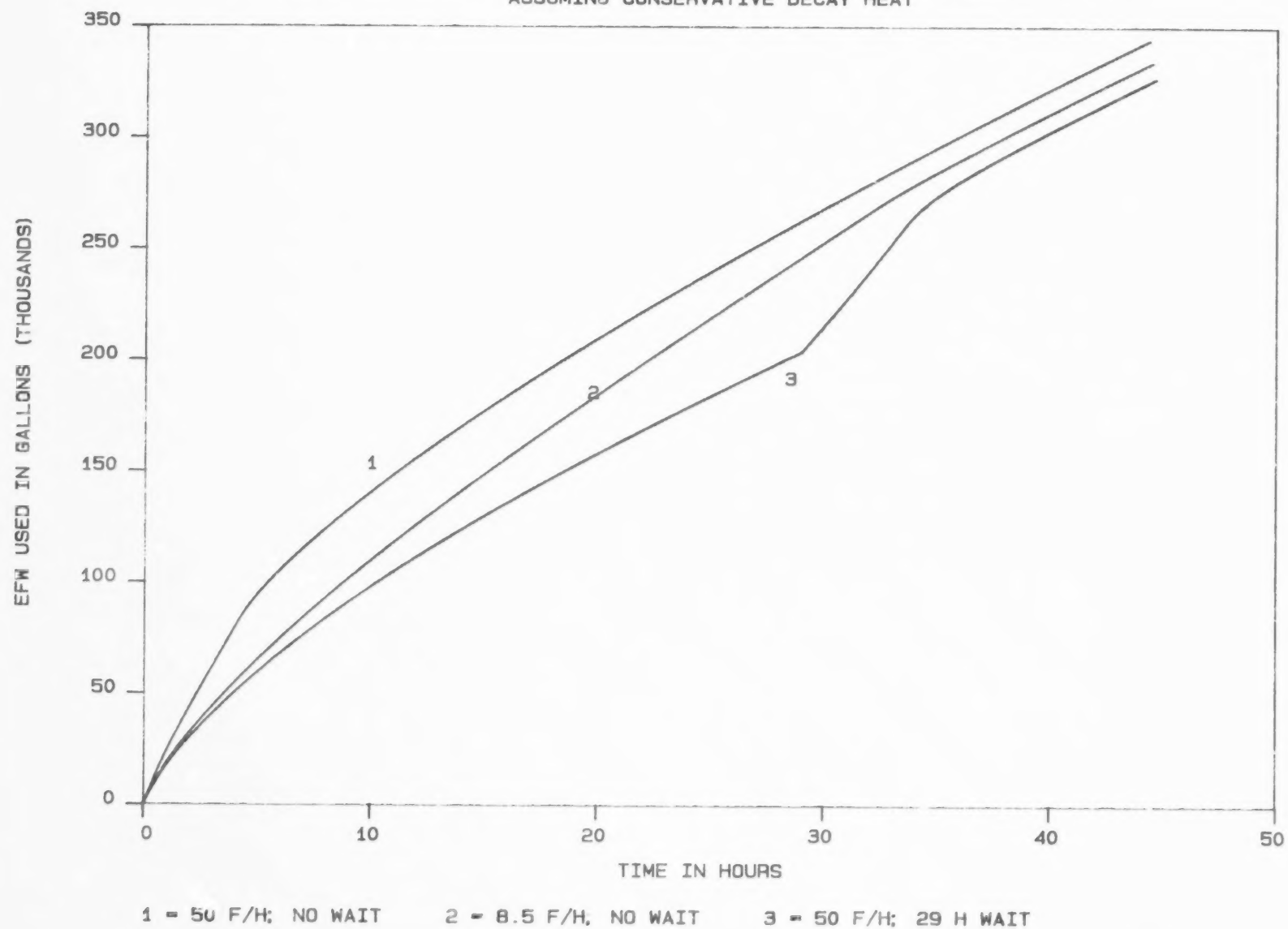
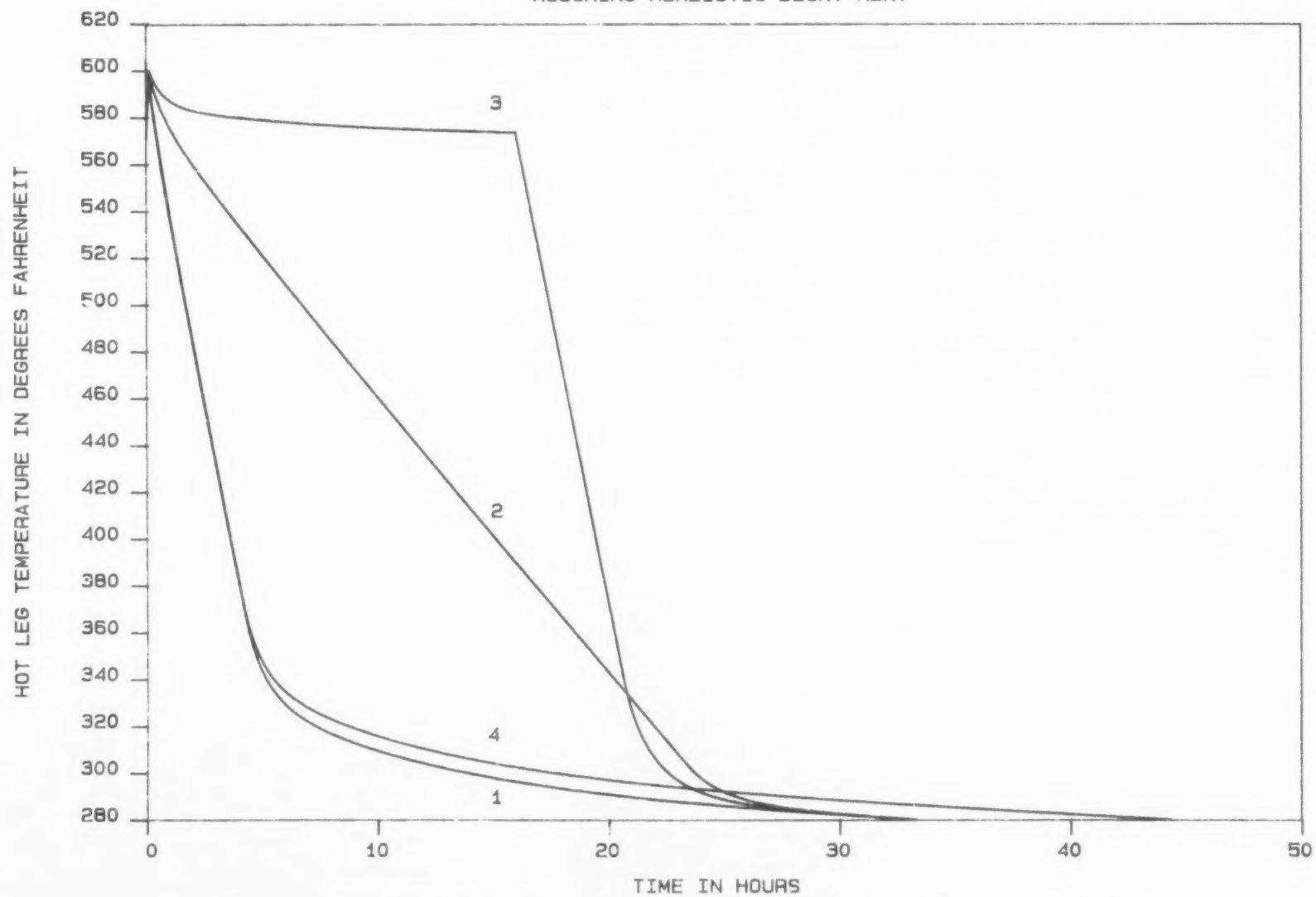


FIGURE 2. EFFECT OF TEMPERATURE-TIME BOUNDARIES ON EFW REQUIREMENTS
ASSUMING CONSERVATIVE DECAY HEAT



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FIGURE 3. TEMPERATURE-TIME BOUNDARIES OF OPERATOR RESPONSE
ASSUMING REALISTIC DECAY HEAT



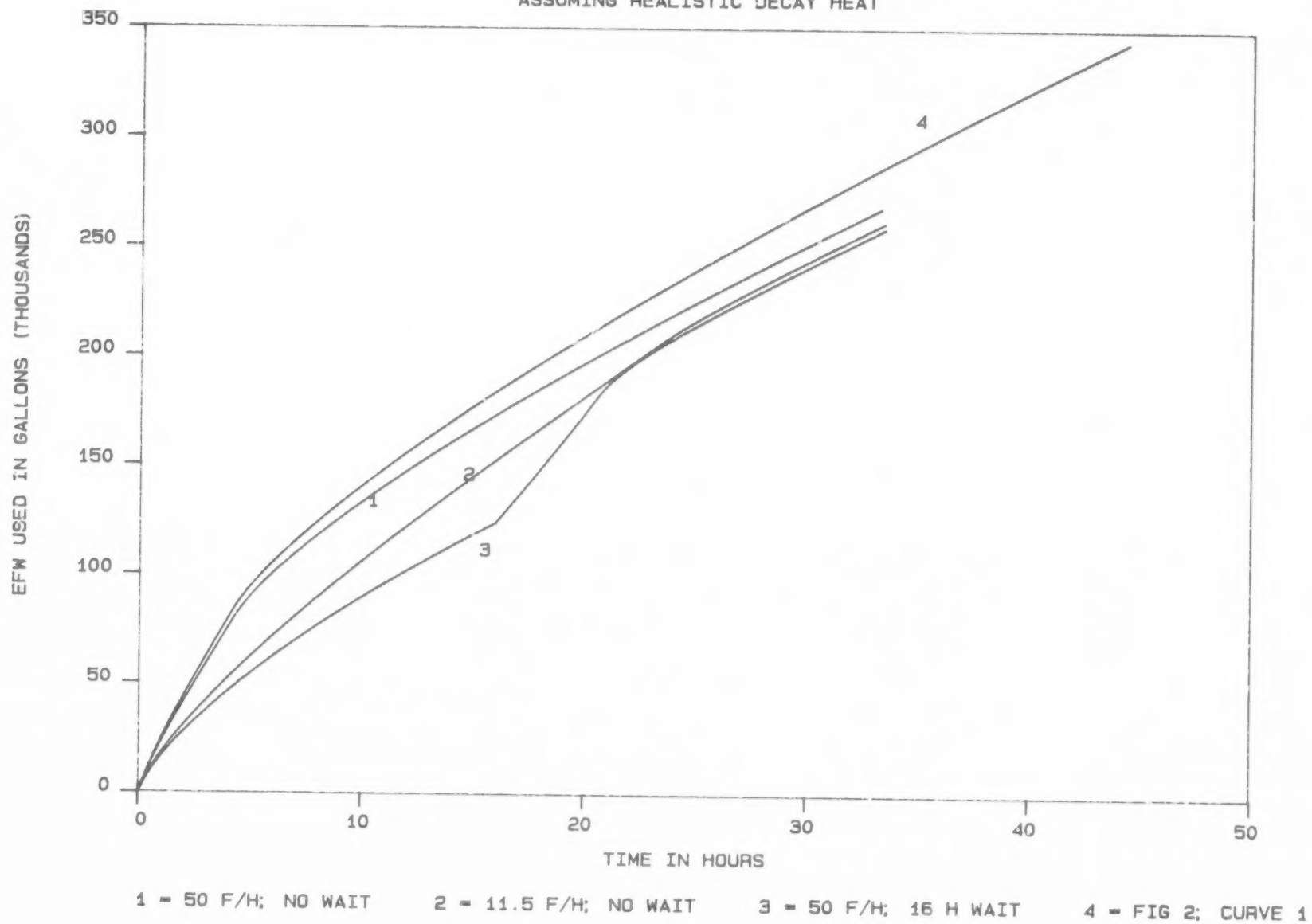
1 - 50 F/H; NO WAIT

2 - 11.5 F/H; NO WAIT

3 - 50 F/H; 16 H WAIT

4 - FIG 1; CURVE 1

FIGURE 4. EFFECT OF TEMPERATURE-TIME BOUNDARIES ON EFW REQUIREMENTS
ASSUMING REALISTIC DECAY HEAT



The influence of another parameter, letdown cooling, is shown in Figure 5. The letdown flow rate used was 100 gpm. The inlet temperature used was the cold leg temperature. Outlet temperature was assumed to be 125F. The effect of continuous letdown cooling is to reduce the minimum cooldown time from about 45 hours to about 28 hours.

Figure 6 shows that continuous letdown cooling during natural circulation cooldown would reduce EFW requirements to about 230,000 gallons, using conservative decay heat.

The influence of potential modifications to the ADVs is shown in Figure 7. Curves 1, 2, and 3 assume conservative decay heat. The CR-3 ADVs, as installed, are actuated only to 45% of total travel. At this setting, about 80% of the ADV mass flow is available. Modifying the existing valves for 100% travel would reduce cooldown time from 45 hours to about 22 hours assuming conservative decay heat and to about 17 hours assuming realistic decay heat. The most rapid cooldown shown in this slide, about 8 hours, occurs when two additional valves (45% travel) are added to the existing two valves (see curve 3).

Figure 8 shows that the curve 3 ADVs (four, 45% travel) would use about 130,000 gallons.

In Figure 9, the influence is shown of filling the steam generator from the normal level of 50% to 95% at a rate of 2% per minute. In curve 1, the filling occurred at a hot leg temperature of 300F. The effect was a rapid decrease in hot leg temperature followed by a return to the reference case plot within about 10 hours. In curve 2, the initiation of filling to 95% at a hot leg temperature of 287F reduces the temperature below 280F, but a time window of only about 1 hour exists prior to the temperature rise in the hot leg to values greater than 280F. The width of the time window increases with time as the temperature decreases. Potentially, with sufficiently rapid response, the existence of the time window might permit initiation of DHR cooling several hours sooner, saving as many as 50,000 gallons of EFW as compared to not filling the steam generator.

Figure 10 shows the EFW inventory requirements associated with the cooldown conditions of Figure 9.

To conclude:

1. Under conditions of loss of offsite power, reactor plants which depend on emergency cooling by exhausting steam through ADVs may experience long times to cooldown.
2. The time to cooldown depends principally on the ADV characteristics and the cut-in conditions of the DHR system.
3. Waiting times at hot standby exist which are a significant fraction of the minimum time to cooldown and do not affect EFW requirements.
4. The amount of EFW required for the minimum cooldown time is almost independent of the cooldown rate.

FIGURE 5. INFLUENCE OF LETDOWN COOLING ON TEMPERATURE
RESPONSE ASSUMING CONSERVATIVE DECAY HEAT

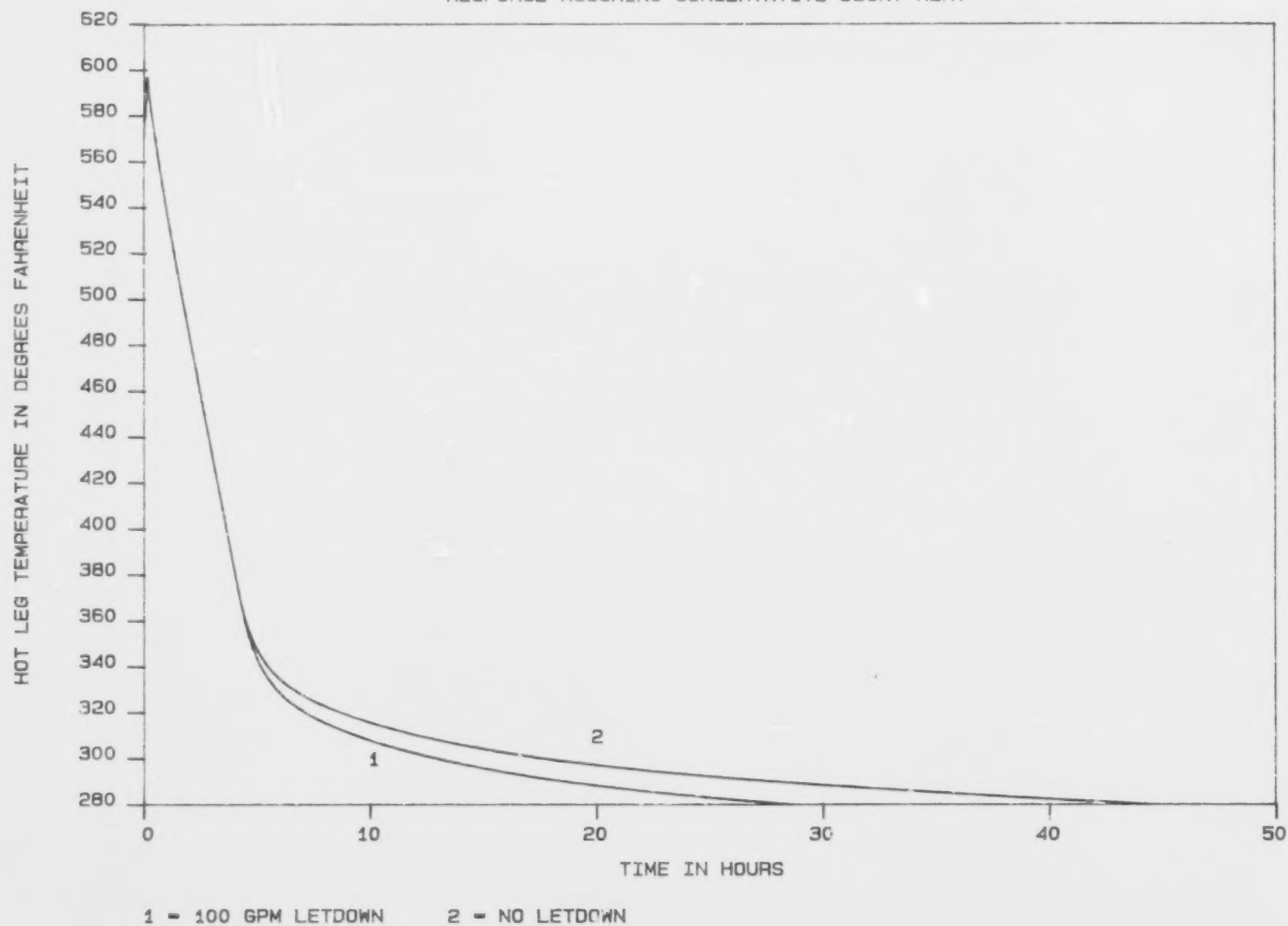
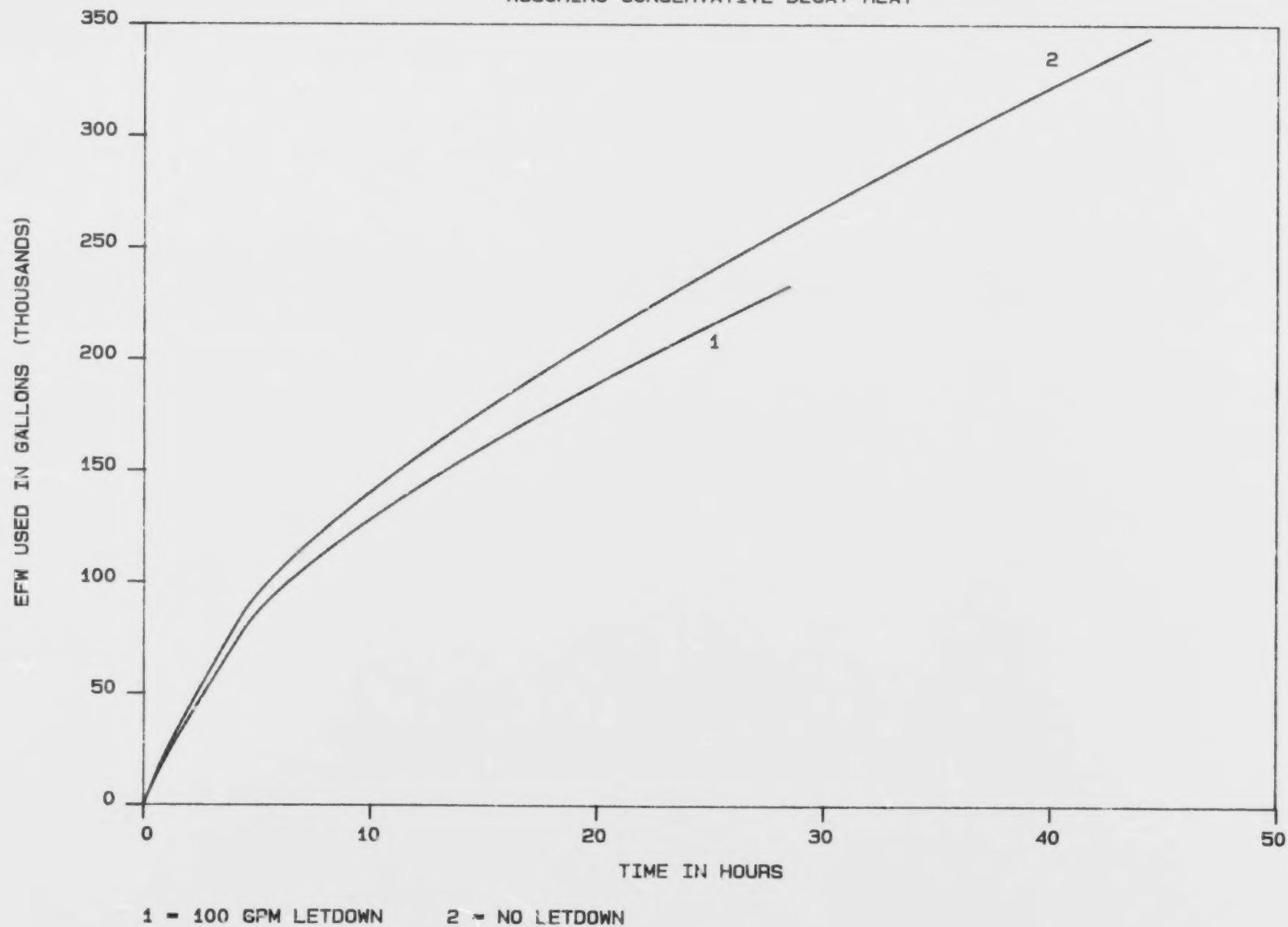


FIGURE 6. INFLUENCE OF LETDOWN COOLING ON EFW REQUIREMENTS
ASSUMING CONSERVATIVE DECAY HEAT



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FIGURE 7. INFLUENCE OF ADV MODIFICATIONS ON TEMPERATURE RESPONSE
ASSUMING CONSERVATIVE DECAY HEAT

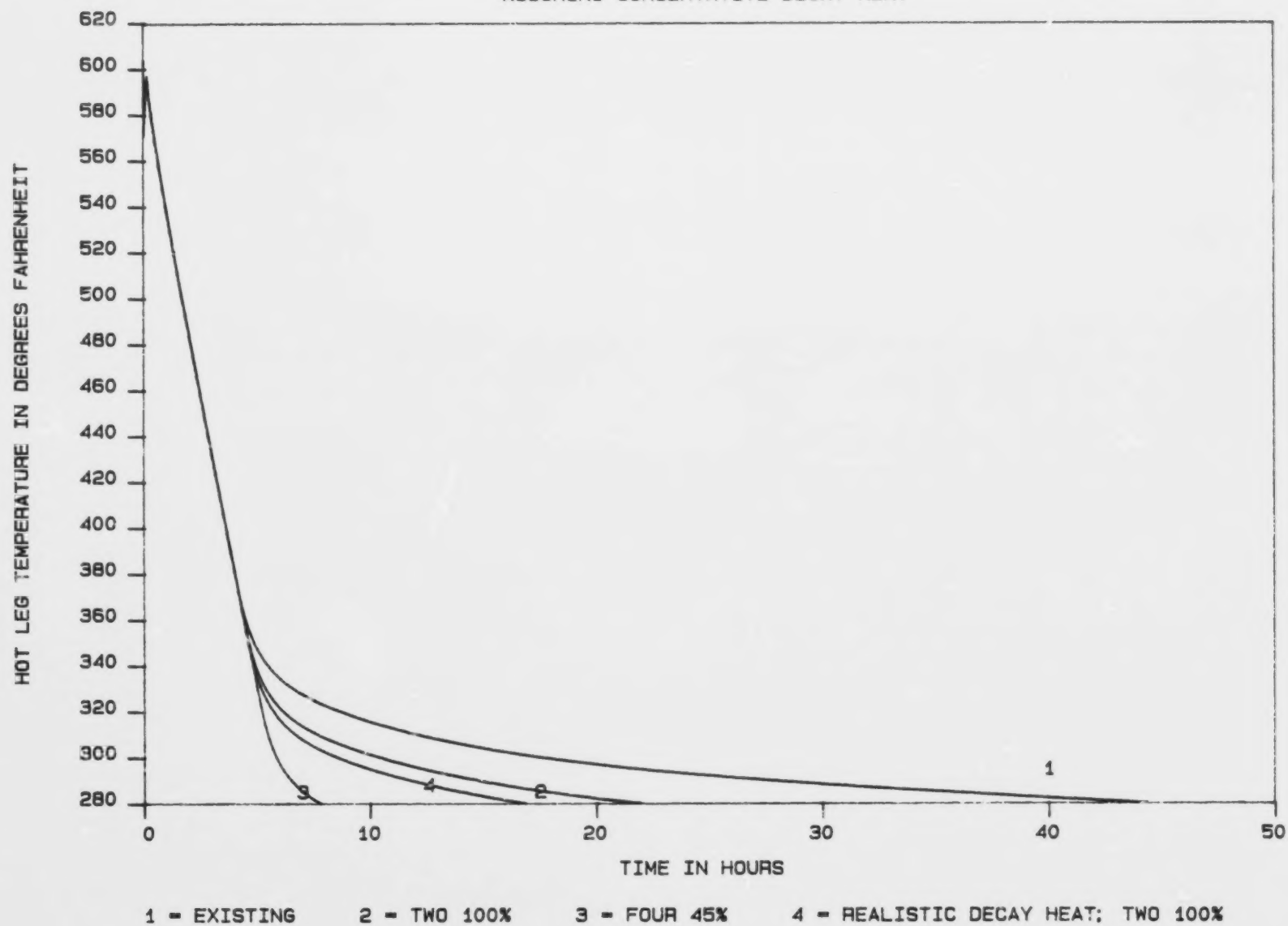


FIGURE 8. INFLUENCE OF ADV MODIFICATIONS ON EFW REQUIREMENTS
ASSUMING CONSERVATIVE DECAY HEAT

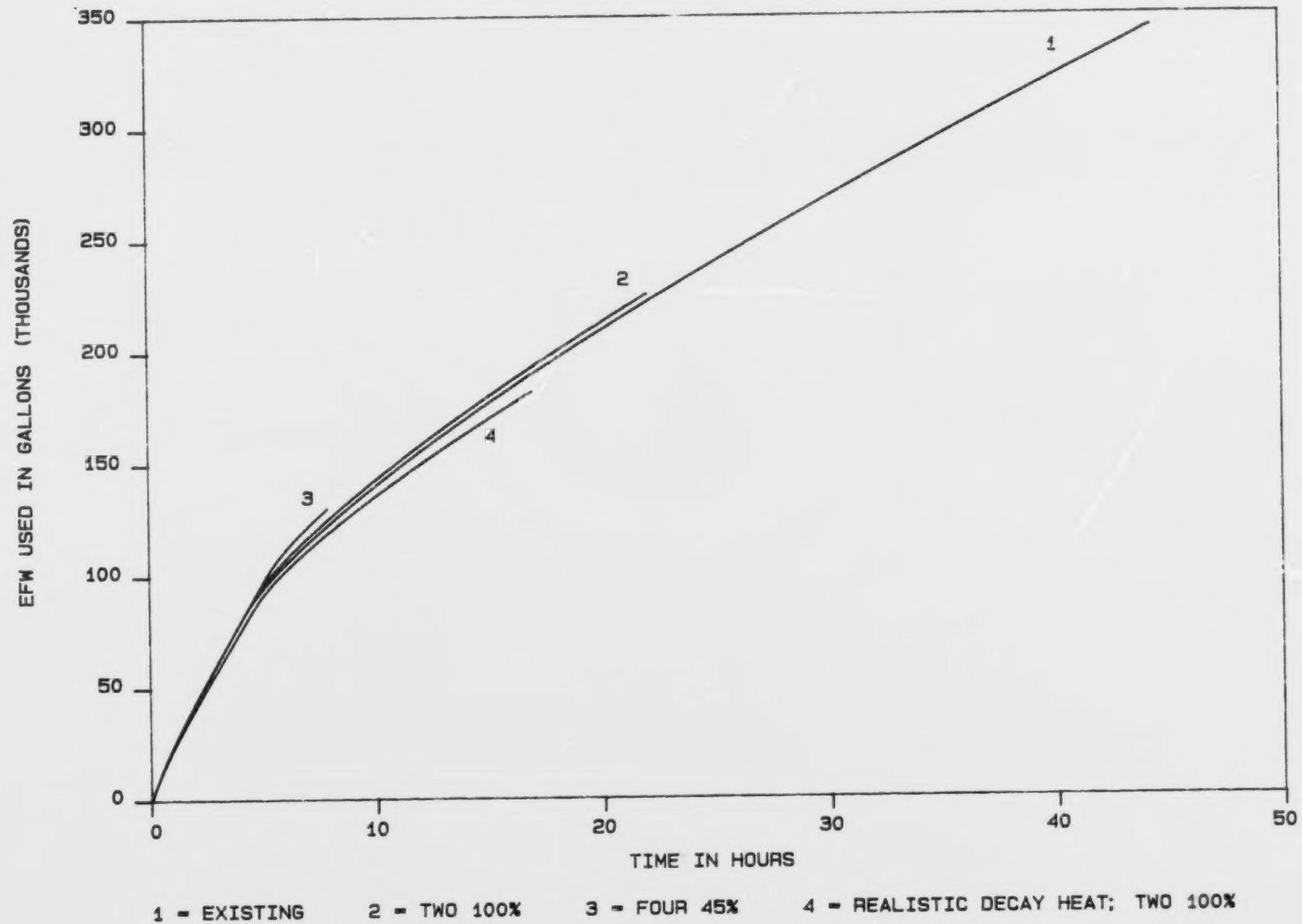


FIGURE 9. INFLUENCE OF OTSG FILL ON TEMPERATURE RESPONSE
ASSUMING REALISTIC DECAY HEAT

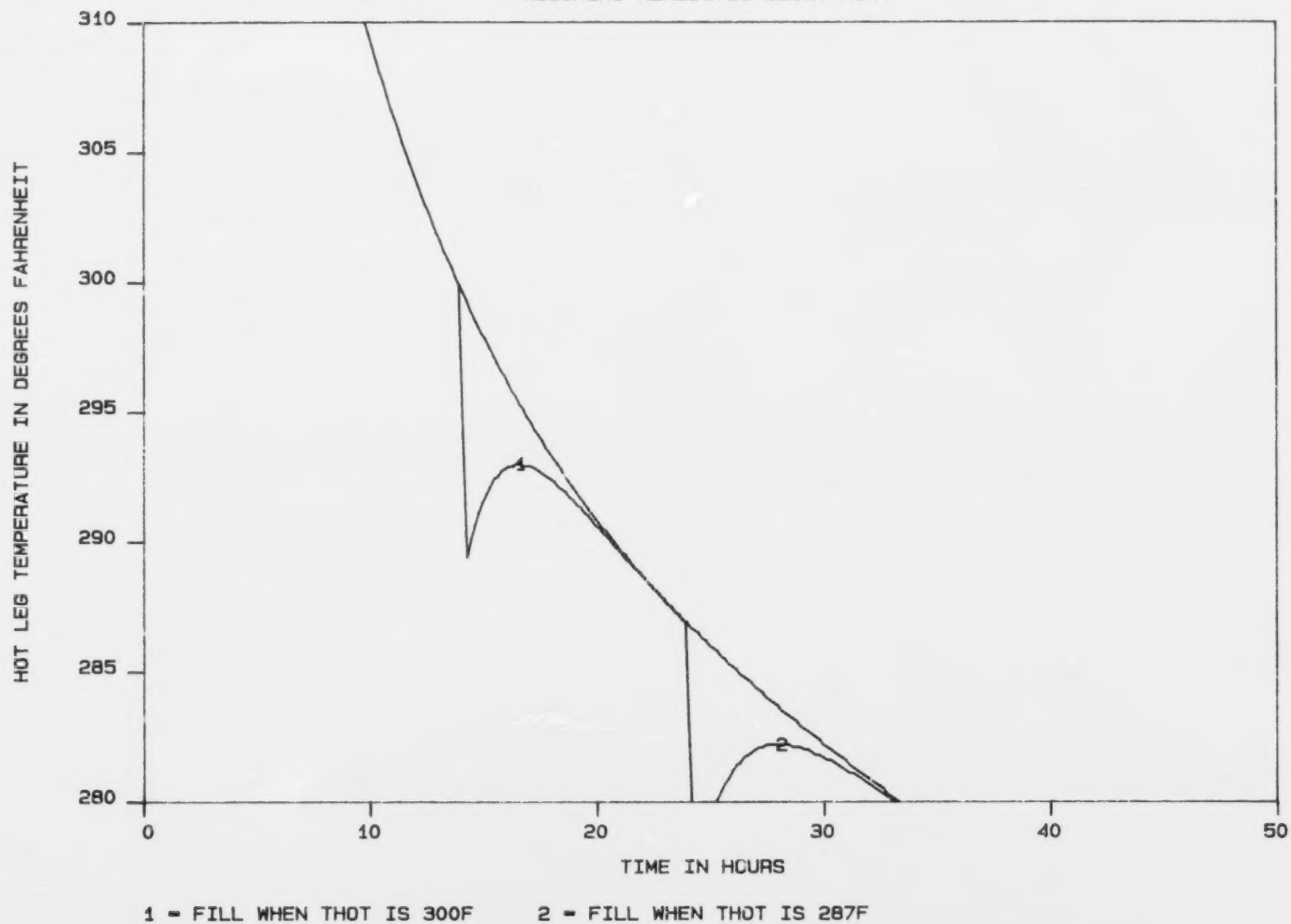
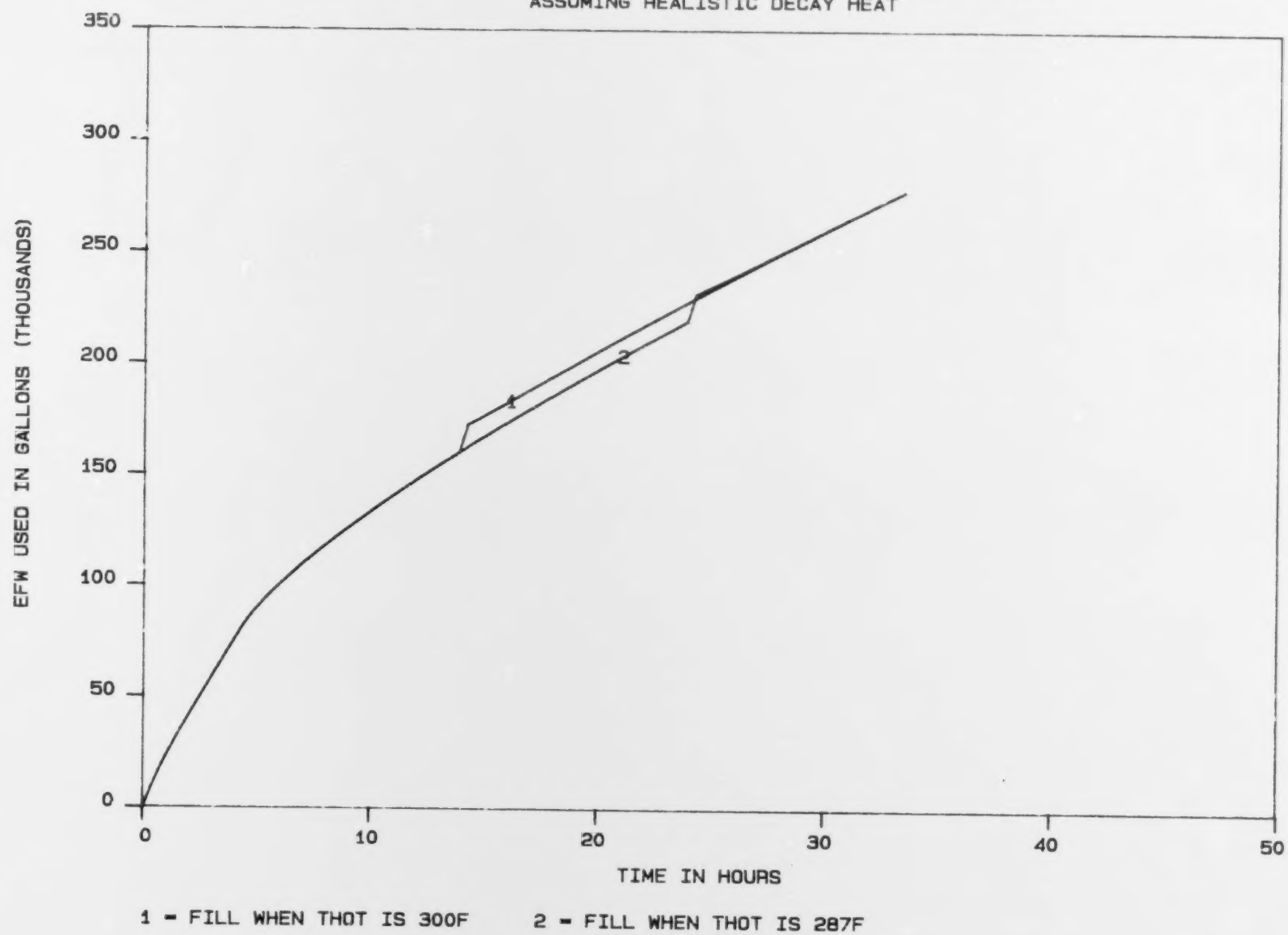


FIGURE 10. INFLUENCE OF OTSG FILL ON EFW REQUIREMENTS
ASSUMING REALISTIC DECAY HEAT



5. Use of letdown coolers during natural circulation cooldown may significantly decrease the time to cooldown and the amount of EFW required.
6. Because the probability of restoring offsite power in a short time is very high (in the absence of weather-related damage), good arguments can be made for remaining at hot standby until the power is restored.
7. Relatively inexpensive options may exist for decreasing the time to cooldown and for reducing EFW requirements. Such options include additional ADVs, use of letdown cooling during natural circulation, and filling of the steam generators late in the cooldown period.

References:

1. U.S. NRC, Natural Circulation Cooldown, Generic Letter 81-21, 18 May 1981.
2. GPUN, TMI-1 Nuclear Generating Station Natural Circulation Cooldown Analysis Without Reactor Vessel Upper Head Void Formation, Topical Report 017, Rev. 1, 3 July 1985.
3. U.S. NRC, "Residual Decay Energy for Light-Water Reactors for Long-Term Cooling", Branch Technical Position ASB 9-2, Standard Review Plan, NUREG-0800, Rev. 2, July 1981.

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TWO-PHASE FLOW MEASUREMENT
IN THE UPPER PLENUM OF A PWR
DURING REFLOOD

by

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ABSTRACT

In order to study the two-phase hydrodynamics of a pressurized water reactor during emergency reflood, a group of four instruments has been developed for installation at the core and upper plenum interface. These instruments are a drag body, a cut-out portion of the end-box tie plate; a turbine meter located above one of the tie-plate holes; a differential pressure measurement across the tie plate; and a collapsed liquid level measurement above the tie plate. A single-module air and water apparatus simulates the reactor during reflood. Tests are done with the upper plenum empty, and with it containing simulated control rod guide tubes and baffle plates.

The flow regimes studied are flooded and unflooded countercurrent flow, upflow, and combined injection. The upper plenum internals have a significant effect on the behavior of the two-phase flow. An algorithm to determine the individual mass flowrates of gas and liquid is presented. This algorithm depends on the drag body force measurement, the differential pressure measurement, the collapsed liquid level, and the flooding line.

INTRODUCTION

The International 2D/3D Refill and Reflood Experimental and Analytical Research Program was formed to study loss-of-coolant accidents in pressurized water reactors. Three countries, Japan, Federal Republic of Germany, and the U.S.A., are cooperating to analyze the steam binding effect during reflood for various core cooling configurations and the flow hydrodynamics in the core, downcomer, and upper plenum. The facilities involved in this project are Japan's Slab Core Test Facility and Cylindrical Core Test Facility and Germany's Upper Plenum Test Facility.

The role of the U.S. is to provide codes to predict three-dimensional effects and instrumentation that will measure the mass flowrates of the liquid and gas phases. In this way the boundary conditions at the interface of the core and upper plenum can be matched among the facilities.

Oak Ridge National Laboratory (ORNL) developed the initial instrumentation scheme. They used three test facilities, known collectively as the Instrument Development Loop. These three were the single-module air and water test loop, the three-module air and water test loop, and the single-module steam and water test loop. The instruments used in these loops were a drag body, turbine meter, string probe, and differential pressure

measurements. They found that the drag body and turbine meter were useful only in the flow regime that consisted of both phases moving upward at a very high velocity.

The Massachusetts Institute of Technology (M.I.T.) was then called upon to determine the flow regimes in which the drag body and turbine meter combination was ambiguous, to evaluate the importance of upper plenum internals on the instrument response and on the flow dynamics, and to provide an algorithm to determine the mass flowrates of each phase. M.I.T. used essentially the same apparatus, a single-module air and water rig, and ran tests with the upper plenum empty and with it containing simulated control rod guide tubes and baffle plates. An algorithm was then developed to predict the superficial velocity of each phase.

M.I.T. was originally asked to provide an algorithm to predict simply the total mass flowrate at the tie plate. This paper, though, presents its algorithm in terms of the superficial velocity of the gas and liquid phases. The physics of the flow hydrodynamics can only be understood by considering what each phase does, and core cooling depends more on individual phase velocities than on the lumped mass flowrate. Figure 1 shows both the regions where M.I.T. believes the instruments' measurements are important and where good core cooling is ensured.

TEST EQUIPMENT

The test equipment consists of the single-module air and water apparatus provided by ORNL. Complete sets of data were taken with an empty upper plenum and with an upper plenum containing simulated Kraftwerk Union (KWU) control rod guide tube structures. The instruments in the apparatus are a free-field turbine meter, a tie-plate drag body, a tie-plate differential pressure cell, and an upper plenum U-tube manometer.

Apparatus with Empty Upper Plenum

The apparatus provided by ORNL represents a full-scale vertical section of the UPTF. It contains a KWU end box that ORNL instrumented and tested in its three-module steam and water test apparatus, a short length of simulated fuel rods, and an upper core support plate. Water can enter the vessel at the hot leg injection outlet, 41 in (1.04 m) above the upper core support plate (UCSP), and at the core spray mixer. Air enters at the core spray mixer. Air and water leave the upper plenum through a 9.25 in (23.5 cm) diameter hole. Water can leave the lower plenum through two 2.5 in (5 cm) diameter exit ports centered 6 in (12 cm) below the upper lip of the mixer. Carryover and fallback are collected in weigh tanks.

The area used in calculating the superficial velocities is the smallest area for flow. In this apparatus it is the tie plate. It is on this area, 0.177 ft² (0.0164 m²), that the gas and liquid superficial velocities are based.

Apparatus with Simulated Internals

This is the same apparatus as used above, but the upper plenum contains simulated control rod guide tubes. In the UPTF control rod guide tubes are centered above half of the end boxes and the guide tubes overhang the

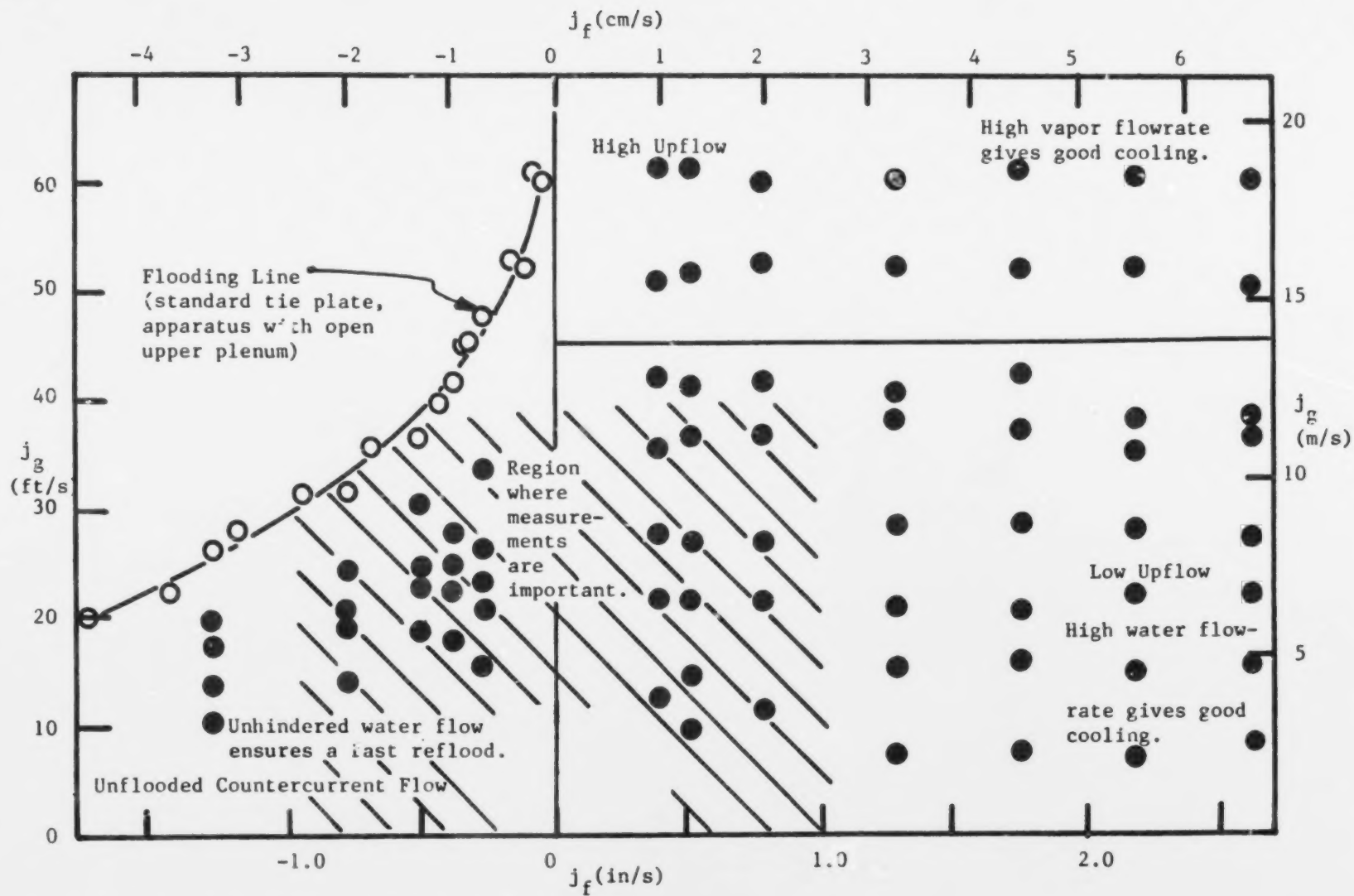


Figure 1. Data Points in M.I.T.'s Study.

adjacent end boxes. The end box in this single-module apparatus simulates one over which overhang the edges of four neighboring guide tubes and their baffle plates. The three-module air and water apparatus designed by ORNL provided the specifications for the guide tubes and baffle plates. M.I.T.'s modified single-module apparatus represents the middle end box in ORNL's rig, with the addition of two more tube segments at the front and back. Two out of eight baffle plates for each guide tube and a 78° section of each 12 in (30 cm) diameter guide tube overhang the end box. The tube segments do not extend all the way up the upper plenum, but end at the bottom lip of the carryover exit hole. Figure 2 shows a side view of this test apparatus.

DISCUSSION OF THE UPFLOW TESTS

Procedure for the Upflow Tests

Upflow is defined as the flow situation in which both the gas and liquid phases are injected at the core spray mixer. The lower plenum is completely sealed so that the net mass flowrate at the tie plate is known.

There are 56 upflow tests for each apparatus. The liquid flowrate is set at seven different values of j_L : 0.051 ft/s (0.016 m/s), 0.068 ft/s (0.021 m/s), 0.103 ft/s (0.031 m/s), 0.169 ft/s (0.052 m/s), 0.230 ft/s (0.070 m/s), 0.288 ft/s (0.088 m/s), and 0.346 ft/s (0.106 m/s). The gas flowrate is then adjusted over the entire range of j_G .

Upflow in the Apparatus with Empty Upper Plenum

Two distinct flow regimes exist in the upflow tests. One is defined as "high upflow." High upflow occurs when the superficial gas velocity is above about 50 ft/s (15 m/s), regardless of the liquid velocity. In this regime, there is no significant pool above the tie plate even though there are pockets of water isolated along the walls above the tie plate, the top of the end box, and the UCSP. No water falls back into the lower plenum from the upper plenum. Water flows from the rod bundle to the tie plate in the form of small droplets that pass through the tie plate holes with little interaction with the tie plate. Thus, there is negligible momentum impingement by the droplets.

The other flow regime is defined as "low upflow." In this regime, which occurs when the gas velocity is below about 50 ft/s (15 m/s), there is a pool above the tie plate and significant water accumulation above the UCSP. Churning water recirculates from the upper plenum into the lower plenum. Substantial blocks of water splash against the bottom of the tie plate, and most of this liquid then passes into the upper plenum. The pool of water also contributes to the fallback into the lower plenum. All fallback from the upper plenum passes through the tie-plate slots along the walls of the test apparatus.

Upflow in the Apparatus with Upper Plenum Internals

There are some differences between the upflow results for the two apparatus. Drag body forces and differential pressures are somewhat greater in the modified apparatus than in the original. There are significant reductions in the collapsed liquid level in the modified apparatus. In high upflow, more

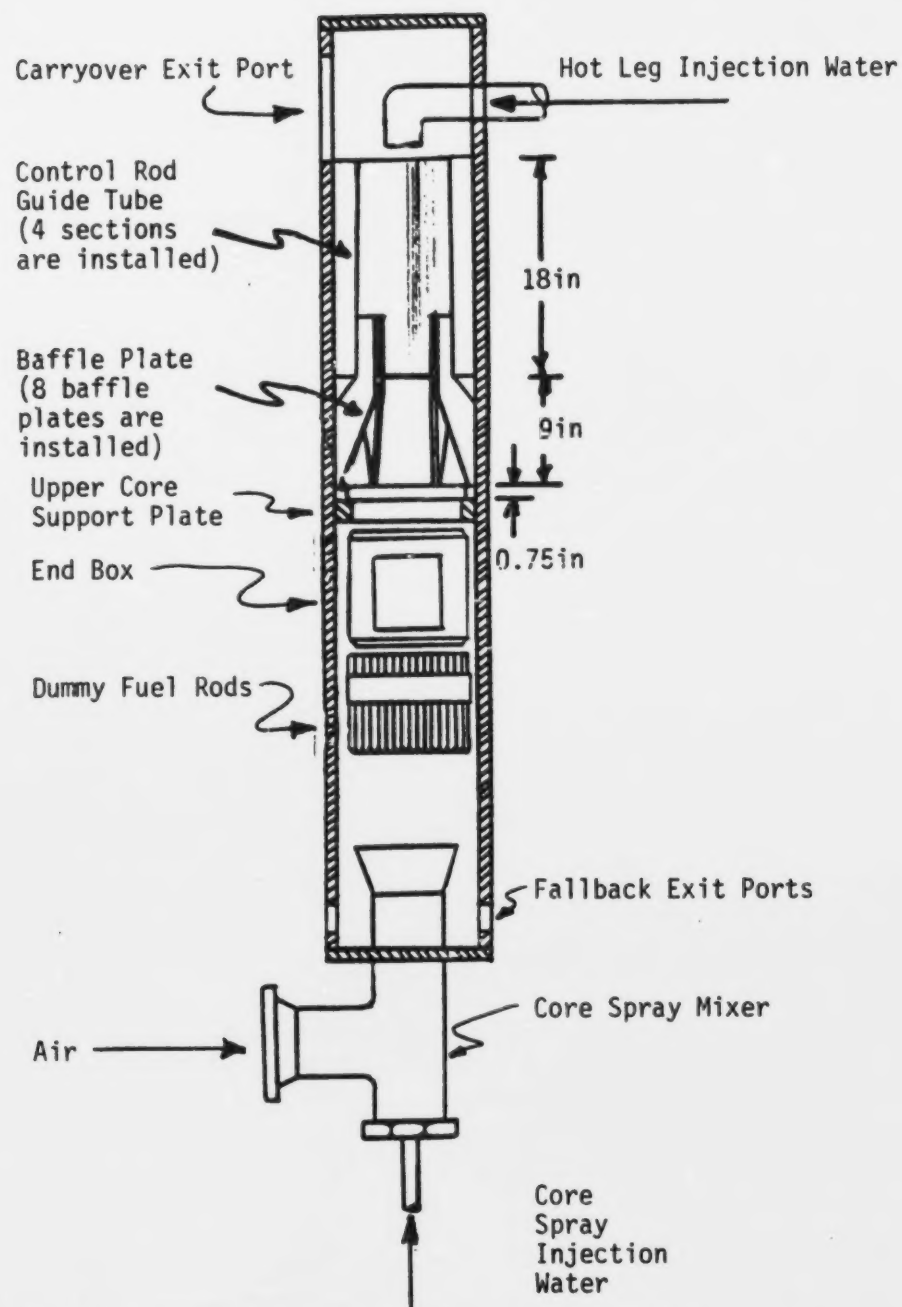


Figure 2. M.I.T.'s Apparatus with Upper Plenum Internals.

water accumulates in the upper plenum of the apparatus with internals than in the one with the empty upper plenum. Water accumulates around the baffle plates and guide tubes. In low upflow, less water accumulates in the upper plenum of the modified than in the original. The guide tubes then encourage the downflow of water into the lower plenum by helping to separate the phases. Air goes through the upper plenum as a gas core and water flows down the walls and simulated internals, through the tie-plate slots, and into the lower plenum.

DISCUSSION OF THE COUNTERCURRENT FLOW TESTS

Procedure for the Countercurrent Flow Tests

In the countercurrent flow tests water is injected via the hot leg, 40 in (102 cm) above the tie plate. The flow regime can be either flooded or unflooded. In the flooded tests water is injected at two different flowrates, 19.2 gpm (0.073 m³/min) and 27.5 gpm (0.104 m³/min), which are high enough so that carryover occurs. The water that penetrates the lower plenum collects in weigh tanks, and the fallback is calculated as a superficial velocity based on the tie plate open area. In the unflooded tests five different water flowrates are injected by the hot leg, 0.036 ft/s (0.011 m/s), 0.051 ft/s (0.016 m/s), 0.068 ft/s (0.021 m/s), 0.103 ft/s (0.031 m/s), and 0.169 ft/s (0.052 m/s), and the gas flowrate is low enough so that there is no carryover. Water leaves the lower plenum by one of the two drainports. During both the flooded and unflooded tests the water level is maintained at about 0.5 ft (15 cm). This level is high enough so that no air escapes through the drainport and it is low enough so that no water is reentrained from the upper lip of the core mixer.

Countercurrent Flow in the Apparatus with Empty Upper Plenum

Figure 3 shows the flooding lines for the two apparatus. The axes are the superficial velocities raised to the 1/2 power; this is the form that Wallis used in single-tube flooding correlations. During flooded operation water first collects above the UCSP. Water then flows past this first area reduction to the tie plate to form a pool. Water enters the lower plenum primarily by the tie plate slots against the walls; the rest of the water flows through freely-draining tie plate holes whilst the air flows mainly through the center holes. Very few of the individual tie plate holes are actually flooded. Some water in the lower plenum collects in the rod bundle at the spacer.

Two correlations express the flooding data. One can correlate the data using the superficial velocities:

$$j_g^{1/2} + (10.36)j_f^{1/2} = 10.53 \quad (1)[\text{English}]$$

where the velocities are in ft/s. One can also correlate the data using the non-dimensional Wallis coordinates:

$$j_g^{*1/2} + (1.952)j_f^{*1/2} = 1.931 \quad (2)$$

where

$$j_g^* = j_g g^{1/2} (g_0 [\rho_f - \rho_g])^{-1/2} \quad (3)$$

and

$$j_f^* = j_f f^{1/2} (gD[\rho_f - \rho_g])^{-1/2} \quad (4)$$

and $D = 0.413$ in (1.05 cm), the diameter of a tie-plate hole.

Because most of the water enters the lower plenum by the tie-plate slots, some tests are done with these slots blocked with modelling clay. Recirculation zones are present on the walls of the apparatus above the tie plate. Water collects in these zones and flows along the walls through the slots. When these slots are blocked, it becomes clear what an important effect they have on flooding. The correlation for the blocked-slots tests is

$$j_g^{*1/2} + (1.479)j_f^{*1/2} = 1.764 \quad (5)$$

in which the superficial velocities are based on the reduced tie-plate area of 0.148 ft^2 (0.0137 m^2). One can see that there is a 24% difference in the slopes and an 8.6% difference in the Y-intercepts between the two correlations. One can also see that for the standard tie plate the flooding limitation velocity is 113 ft/s (34.4 m/s) but for the blocked-slots tie plate it is 94.5 ft/s (28.8 m/s).

Countercurrent Flow in the Apparatus with Upper Plenum Internals

The upper plenum structures have significant effects in the flooded and unflooded flow regimes. In the flooded tests the tube segments and baffle plates tend to separate the phases. Water flows down the walls of the apparatus within the tube segments, then past the UCSP to the tie-plate slots and to the lower plenum. Practically all the water that enters the lower plenum goes through the tie plate slots. The air flows almost as a gas core through the center of the upper plenum.

The flooding correlations change with the addition of the upper plenum internals. Figure 3 shows the flooding lines for both apparatus. The correlation for the modified apparatus is

$$j_g^{1/2} + (25.29)j_f^{1/2} = 15.52 \quad (6)[\text{English}]$$

in which the slope is 144% steeper than the original's and the Y-intercept is 47% greater. This correlation also yields a flooding limitation velocity of 241 ft/s (73.4 m/s). The S-shape of the two flooding lines is similar, but in the case of the modified upper plenum it appears that the apparatus is barely in a flooded flow regime. One can also express the flooding relation in Wallis coordinates:

$$j_g^{*1/2} + (4.769)j_f^{*1/2} = 2.853. \quad (7)$$

Thus, at high gas velocities, i.e., j_g greater than 50 ft/s (15 m/s), there is more liquid downflow in the modified plenum than in the original one, but at low gas velocities, i.e., j_g less than 50 ft/s (15 m/s), there is less liquid downflow. At the high gas velocities the upper plenum internals encourage the downflow of water by separating the phases and directing the water down through the slots by the UCSP and the tie plate. At the low gas velocities, though, the outside diameters of the tube segments direct the hot leg injection stream to the center of the tie plate. The center tie plate

holes are never flooded, so liquid downflow is inhibited.

There are also major differences during unflooded operation. When the upper plenum is empty the hot leg injection stream strikes an upper plenum wall, then flows down the wall past the UCSP and through the tie-plate slots to the lower plenum. The tube segments, though, prevent the stream from reaching the wall and direct it instead to the center of the tie plate where the turbine meter is located. The stream causes the turbine meter to give negative-velocity readings.

DISCUSSION OF THE COMBINED INJECTION TESTS

Procedure for the Combined Injection Tests

In the combined injection tests both the hot leg and the core spray mixer supply water. The lower plenum is sealed so that there is no ambiguity in the mass flowrate that the instruments sample. At steady state the instruments measure the net flowrates of air and water supplied by the core spray mixer, even though some water from the hot leg may pass through the lower plenum.

Combined Injection in the Apparatus with Empty Upper Plenum

Observation shows that there is little difference between combined injection and upflow. At high gas velocities no hot leg injection water reaches the lower plenum. At low gas velocities the flow regime resembles churning low upflow. Upper-plenum water accumulation, though, is somewhat greater in the combined injection tests than in the upflow tests.

Combined Injection in the Apparatus with Upper Plenum Internals

As for the original apparatus, the combined injection tests in the modified apparatus are similar to the upflow runs. Somewhat more water than in the original apparatus is held up in the upper plenum because of the baffle plates.

ALGORITHM

The algorithm for the air and water apparatus depends on the measurements of the differential pressure transducer, the collapsed liquid level, the drag body, and the turbine meter. The algorithm is graphical. The upflow algorithm, which uses the differential pressure and collapsed liquid level to determine the superficial velocity of each phase, gives unambiguous results for j_g and j_f . The countercurrent flow algorithm, though, gives clear results during flooding but ambiguous outputs of j_f when unflooded. A default value has been assigned to j_f in that flow regime. Figure 1 is a flow map of the different flow regimes. With the exception of the flooding line, the lines separating the flow regimes are the same for both apparatus.

The air and water algorithm can distinguish between countercurrent flow and upflow. One must use the turbine meter velocity and drag body force to make this distinction. Because the turbine meter is prone to mechanical problems and is very sensitive to the water channelling effects of the upper plenum structures, it is prudent to provide another instrument to distinguish

between upflow and countercurrent flow. When this algorithm is applied to a steam and water system one additional measurement, a temperature reading below a strategic tie-plate hole, can be added to the instrument group. That additional sensor has been used by other researchers of two-phase reactor hydrodynamics to distinguish between countercurrent flow and upflow.

Distinguishing between Upflow and Countercurrent Flow

One can use the drag body force and turbine meter velocity to distinguish between upflow and countercurrent flow. Two criteria must be used: one for gas velocities between 0 and 50 ft/s (15 m/s) and another for velocities between 50 and 100 ft/s (15 and 31 m/s).

In the range of gas velocities between 0 and 50 ft/s (15 m/s), the drag body force will be between 0 and 1.2 lbf (5.3 N) for flooded and unflooded countercurrent flow. But in upflow, the drag body force never goes below 1.2 lbf (5.3 N). At gas velocities greater than 50 ft/s (15 m/s) there is no distinction between the two flow regimes with only the drag body force measurement.

One must take the ratio of turbine meter velocity (V_{tm}) divided by drag body force (F_{db}) as the criterion for distinguishing between the regimes. The ratio of V_{tm}/F_{db} will be high in countercurrent flow and low in upflow. The lowest value for this ratio in countercurrent flow is greater than the highest value in upflow. Thus, for gas velocities between 50 and 100 ft/s (15 and 31 m/s) the following criterion can be used:

$$\text{If } F_{db} < (0.1 \text{ lbfs/ft}) V_{tm} \quad (8) [\text{English}]$$

Then the flow is upflow.

Also, the instruments can detect downflow, an unlikely flow regime. If drag body force goes below zero then the flow must be downflow.

A backup for the turbine meter and drag body measurements is a well-placed thermocouple underneath the tie plate. This thermocouple can be used in a steam and water apparatus. When the flow is countercurrent flow or downflow the temperature will be intermittently the subcooled injection water temperature. When the flow is upflow, the thermocouple will read the saturation temperature. A subsequent section details the placement of these thermocouples.

Upflow Algorithm

The upflow algorithm depends on the differential pressure (DP) and the collapsed liquid level (CLL), two independent readings, to find the gas and liquid superficial velocities. Figures 4 and 5 are plots of DP vs. CLL. Both plots feature the same data points and curves, but they have been drawn twice to show separately the error bands for j_g and j_f . A sensitivity analysis of the algorithm produced the error bands on the lines of constant j_g and j_f . The bands, oriented around the best-fit curve through the data points, contain the data points for constant j_g and j_f and thus portray the amount of scatter in the data. These bands can be expressed as percentages of the j_g or j_f . As an example, in Figure 4, for $j_g = 42$ ft/s one can go 2% above the best-fit line and 6% below and still be within the range in the

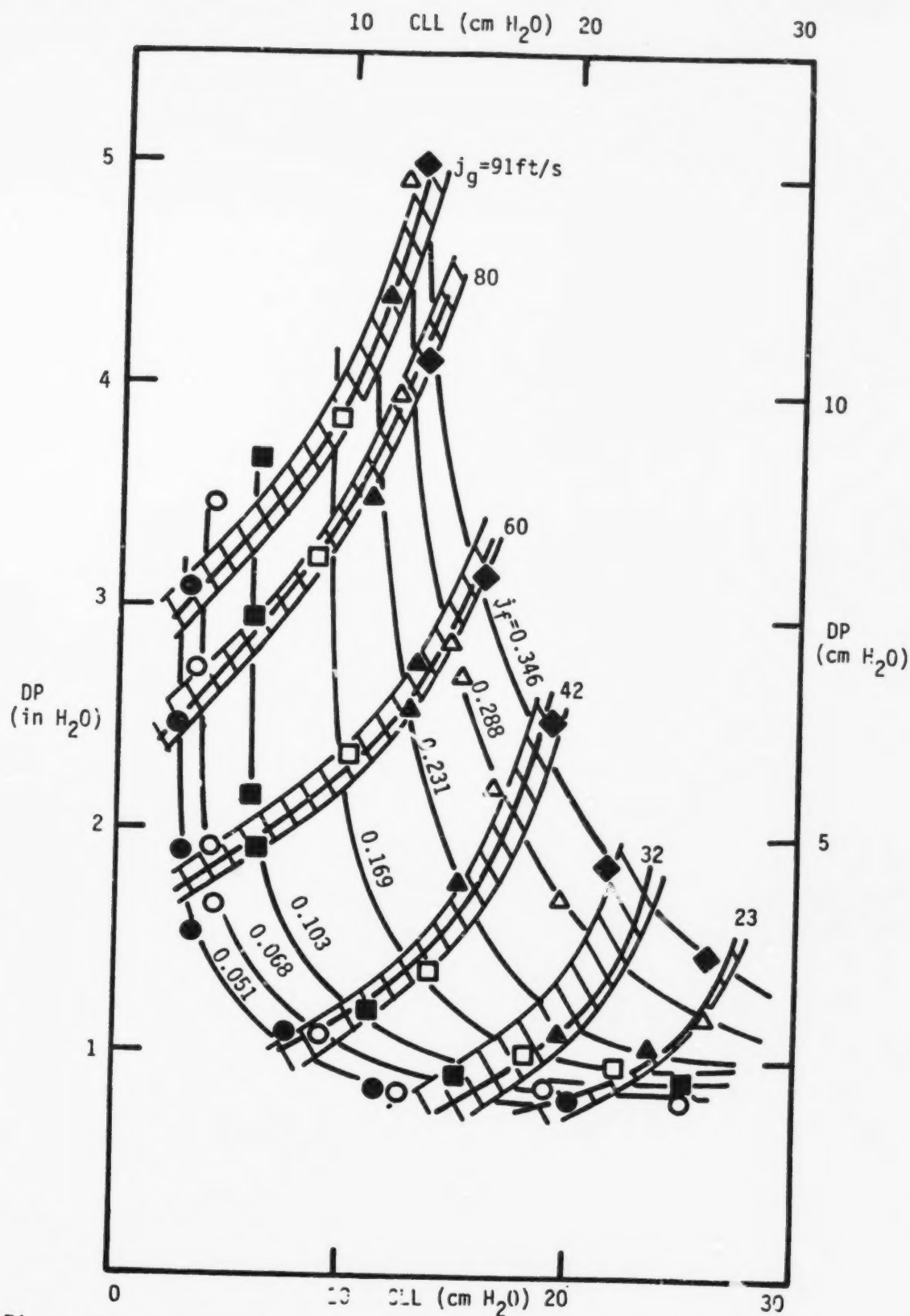


Figure 4. Upflow Algorithm

data of 42 ft/s. One can see that the bands do not overlap but merge in the lower right corner of Figure 5. Thus, the upflow algorithm is unambiguous.

Once one knows that the flow is upflow, one needs only to find the point of interest on this graph. The air and water superficial velocities can then be read off.

Countercurrent Flow Algorithm

In the countercurrent flow algorithm one needs first to find if the flow is flooded or unflooded. If the collapsed liquid level is 2cm or greater, then the apparatus is flooded. If the CLL is less than this value, then the apparatus can only be unflooded. One then uses the drag body force measurement to determine the value of j_g , for either flooded or unflooded flow. Figure 6 is a plot of F_{db} vs. j_g for flooded and unflooded countercurrent flow. As for the upflow algorithm, a sensitivity analysis gives the error bands on Figure 6.

If the apparatus is flooded, one then uses this value of j_g to determine j_f by the flooding line, Figure 3. Since the curve is somewhat S-shaped, it should be better to read the value of $j_f^{1/2}$ directly off of the graph than to attempt a linear curve-fit.

If the apparatus is not flooded, then there is ambiguity in the liquid flowrate. All the data for the different water velocities fall into the same line. One must then assign a default value, which is the average of all the liquid superficial velocities: 0.1025 ft/s (0.0313 m/s).

Figure 7 is a flowchart that summarises the algorithm.

A Steam and Water Algorithm

The air and water algorithm can be extended to a steam and water system and an additional device can be added to distinguish between upflow and countercurrent flow. This device is a temperature sensor that must be located underneath a tie-plate hole. The hole must be chosen carefully so that when the apparatus is in upflow, the sensor will read the saturated steam temperature. When the apparatus is in countercurrent flow, either flooded or unflooded, the sensor will read the subcooled injection water temperature.

Liquid downflow into the lower plenum during countercurrent flow occurs beneath certain structures above the tie plate. In the air and water apparatus, most liquid downflow is through the tie-plate slots along the apparatus' walls. Likewise, in the steam and water apparatus most liquid downflow will occur underneath structures that encourage liquid accumulation. These structures will be walls or other internals that are on or very near the tie plate. Pockets of dead fluid will develop around end box corners and water will flow down from pools above the tie plate and UCSP, especially around the baffle plates. Many thermocouples could be placed in these spots throughout the steam and water apparatus, since in the UPTF there will probably be areas of pure upflow and others of complete coolant dumping.

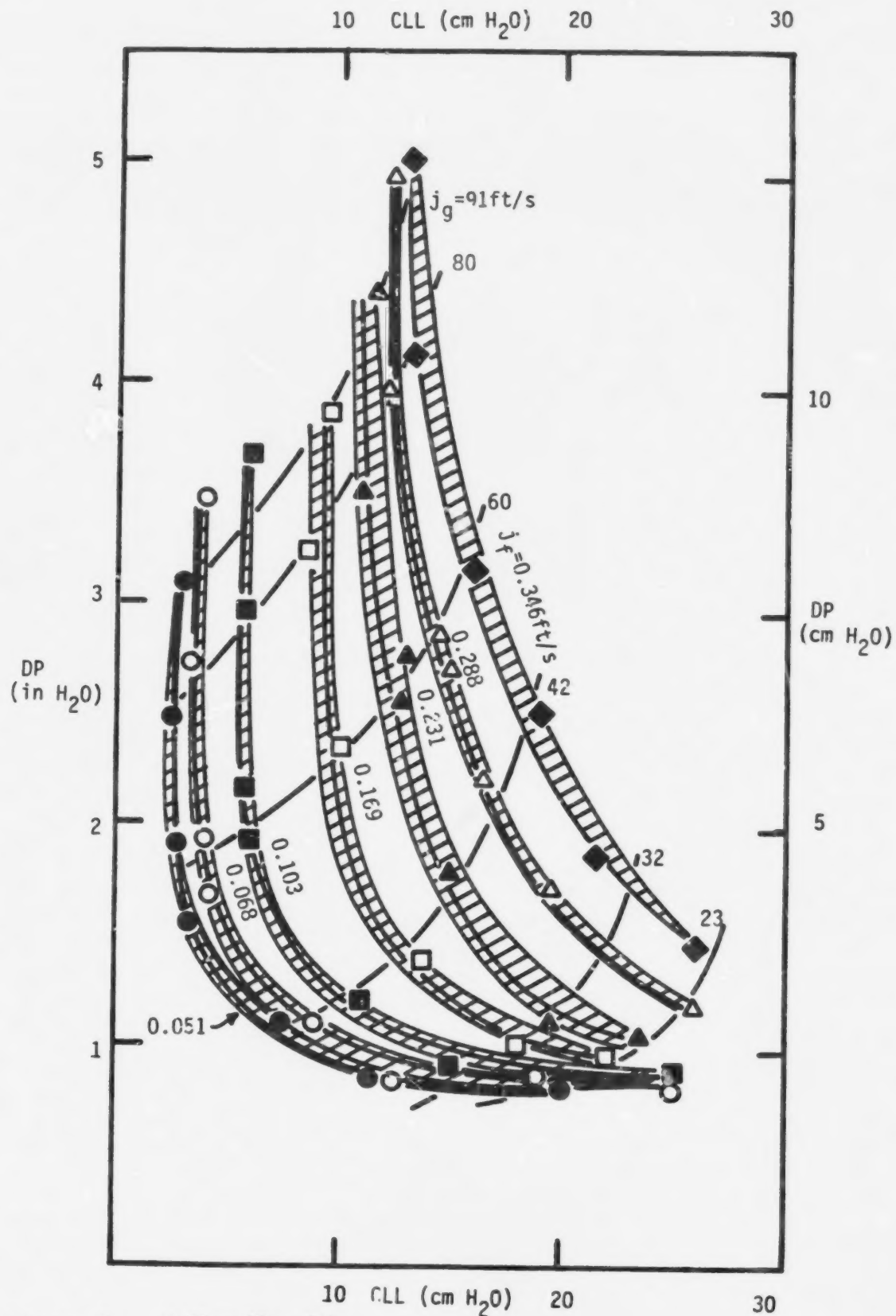


Figure 5. Upflow Algorithm.

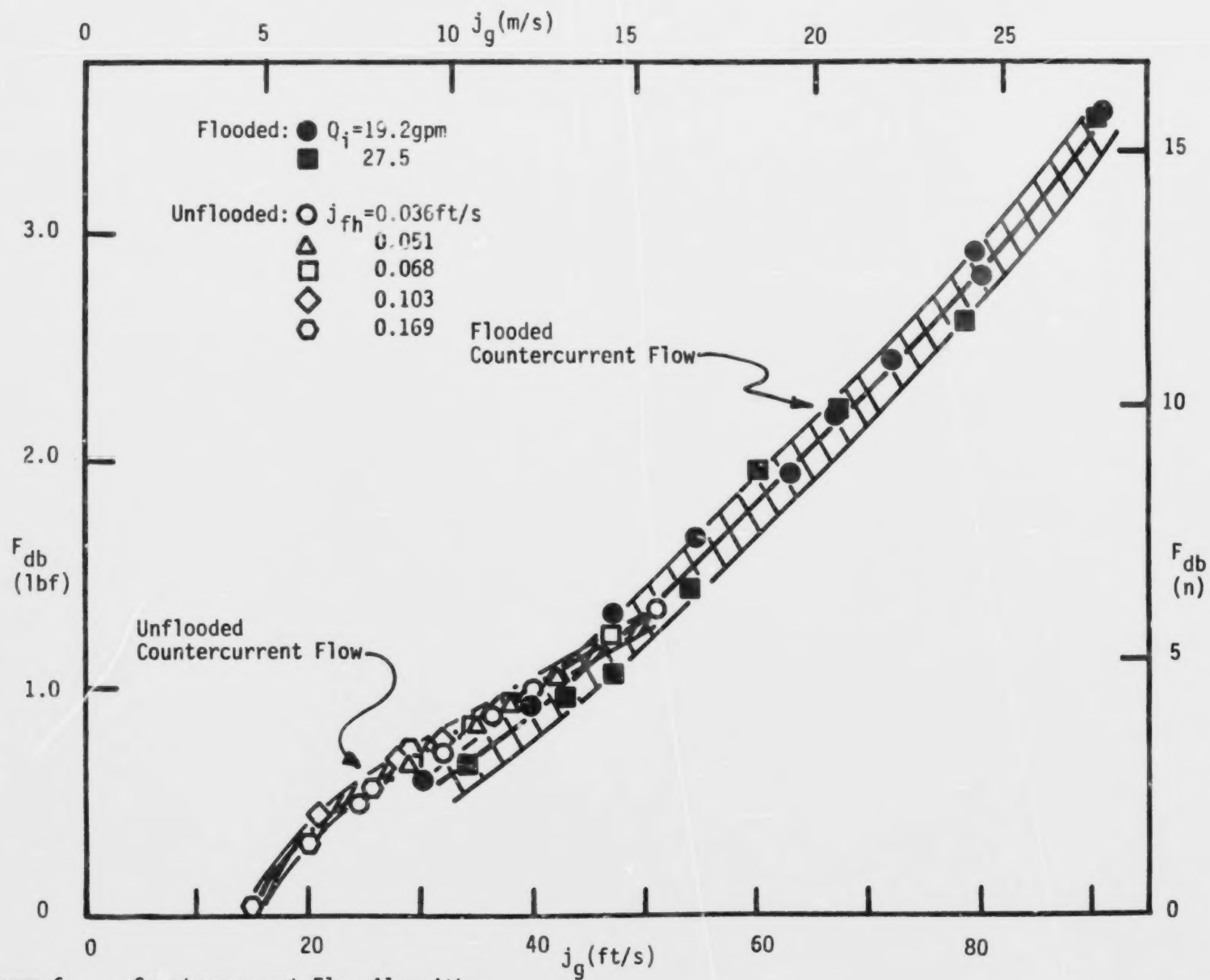


Figure 6. Countercurrent Flow Algorithm.

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CONCLUSIONS

Conclusions can be made in three areas: instrument performance, effects of geometry and experiment scaling, and the algorithm. First, in instrument performance, one can conclude that the turbine meter is the least reliable instrument of the group. Secondly, the effects of geometry and scaling include the strong influence of the tie-plate slots, the apparatus' walls, and the upper-plenum internals on flooding behavior and general hydrodynamics. Finally, the algorithm, which can distinguish between upflow and countercurrent flow, gives good results for gas and liquid superficial velocities in upflow and countercurrent flooded flow. In unflooded flow, it produces j_g but it requires a default value, 0.1025 ft/s (0.0313 m/s) for the superficial liquid velocity. But one can calculate the liquid downflow in the unflooded regime by simply balancing the liquid mass flowrates in the upper plenum.

Instrument Performance

The turbine meter is unreliable in our tests. It continually poses mechanical problems with particulates entering the bearing races or with the capsule loose in the housing. It also responds poorly in many flow regimes. In upflow it stalls when a pool is present above the tie plate, at gas velocities below about 50 ft/s (15 m/s). In countercurrent flow it does not work well at gas velocities below about 30 ft/s (9.2 m/s). At these gas velocities water splashes in the meter directly from the hot leg above, sometimes causing it to stall. At gas velocities above about 45 ft/s (14 m/s), the turbine responds to single-phase air because the water is channelled through the tie-plate slots. This behavior is important in the algorithm for the air and water apparatus, though, because it helps distinguish between upflow and countercurrent flow. But because the turbine meter is very sensitive to the flow-channelling effect of the upper plenum internals and the apparatus' walls it is difficult to predict what will happen in an apparatus that does not have these scaling problems. For these reasons the turbine meter is included in the algorithm, but with reservations. A better way to distinguish between flow regimes is to use thermocouples below the tie plate.

Effects of Geometry and Scaling

In this work two geometric effects are studied: the importance of the tie-plate slots for liquid penetration into the lower plenum and the effects of upper plenum control rod guide tube structures.

One can see the effects of blocking the tie-plate slots in the flooding lines. In the plot of $j_g^{1/2}$ vs. $j_f^{1/2}$, Figure 3, the superficial velocities are based on the tie-plate areas for the standard tie plate, 0.177 ft² (0.0164 m²) and for the blocked-slots tie plate, 0.148 ft² (0.0137 m²). Thus, the change in flooding behavior on the plot can only be due to the blocking of the slots. The water that is channelled by the apparatus' walls is influenced greatly by the blocking of the slots.

The upper-plenum internals enhance the channelling effect. In flooded flow and upflow, liquid penetration into the lower plenum is greater with the internals than without. This is because the guide tubes and baffle plates

channel water along the apparatus' walls. One can conclude this from observation and from the data: during flooding there is greater liquid downflow with internals than without at gas velocities greater than 50 ft/s (15 m/s), and in upflow the collapsed liquid level is less with internals than without at gas velocities less than 40 ft/s (12 m/s).

On the contrary, in upflow where the gas velocities are above 45 ft/s (14 m/s) the baffle plates encourage the accumulation of water above the upper core support plate. The collapsed liquid level readings are higher with internals than without in this flow regime. The flooding data also reflect this accumulation effect: at gas velocities below 50 ft/s (15 m/s) there is less water downflow in the modified apparatus than in the original.

Finally, in unflooded countercurrent flow the hot leg injection stream is kept from hitting the apparatus' walls by the control rod guide tube. The tube directs the stream directly into the turbine meter, which then records negative velocities.

Thus, the two-phase flow behavior is dominated by walls that will not be in a large-scale apparatus. The channelling effect of the walls and the tube segments will not be present in the full-size reactor set-up. Nevertheless, the conclusions and algorithm developed from the single-module air and water apparatus should provide a rough guide for experimenters at the full-size apparatus.

The Algorithm

In upflow and flooded countercurrent flow the algorithm provides unambiguous answers for the air and water superficial velocities. The drag body force, collapsed liquid level, differential pressure and flooding line are used in these flow regimes.

In unflooded countercurrent flow, there are clear results for the air superficial velocity, but a default value is assigned to the water superficial velocity. The instruments cannot distinguish among the different water flowrates in this flow regime.

When the algorithm is used in the steam and water apparatus, it is recommended to use an additional instrument to distinguish between upflow and countercurrent flow. It is possible to use the air-and-water-apparatus algorithm to define the flow regime. But because one of the criteria for making this decision depends on the turbine meter a back-up instrument should be considered for installation. This fifth instrument is a thermocouple placed below a tie-plate hole. The thermocouple, if placed correctly, should read the saturation temperature during upflow and intermittently the subcooled injection water temperature during flooding.

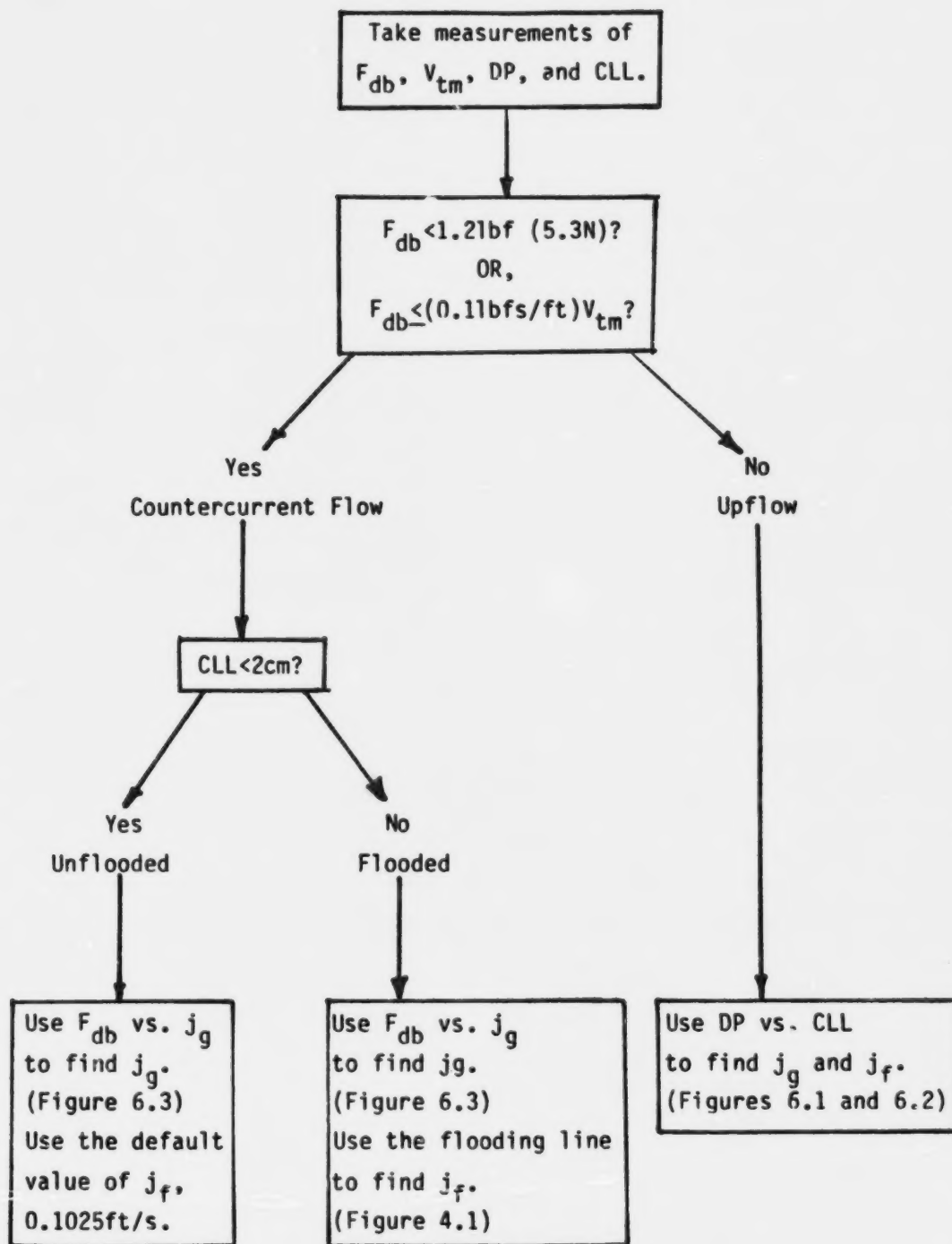


Figure 7. Summary of the Algorithm.

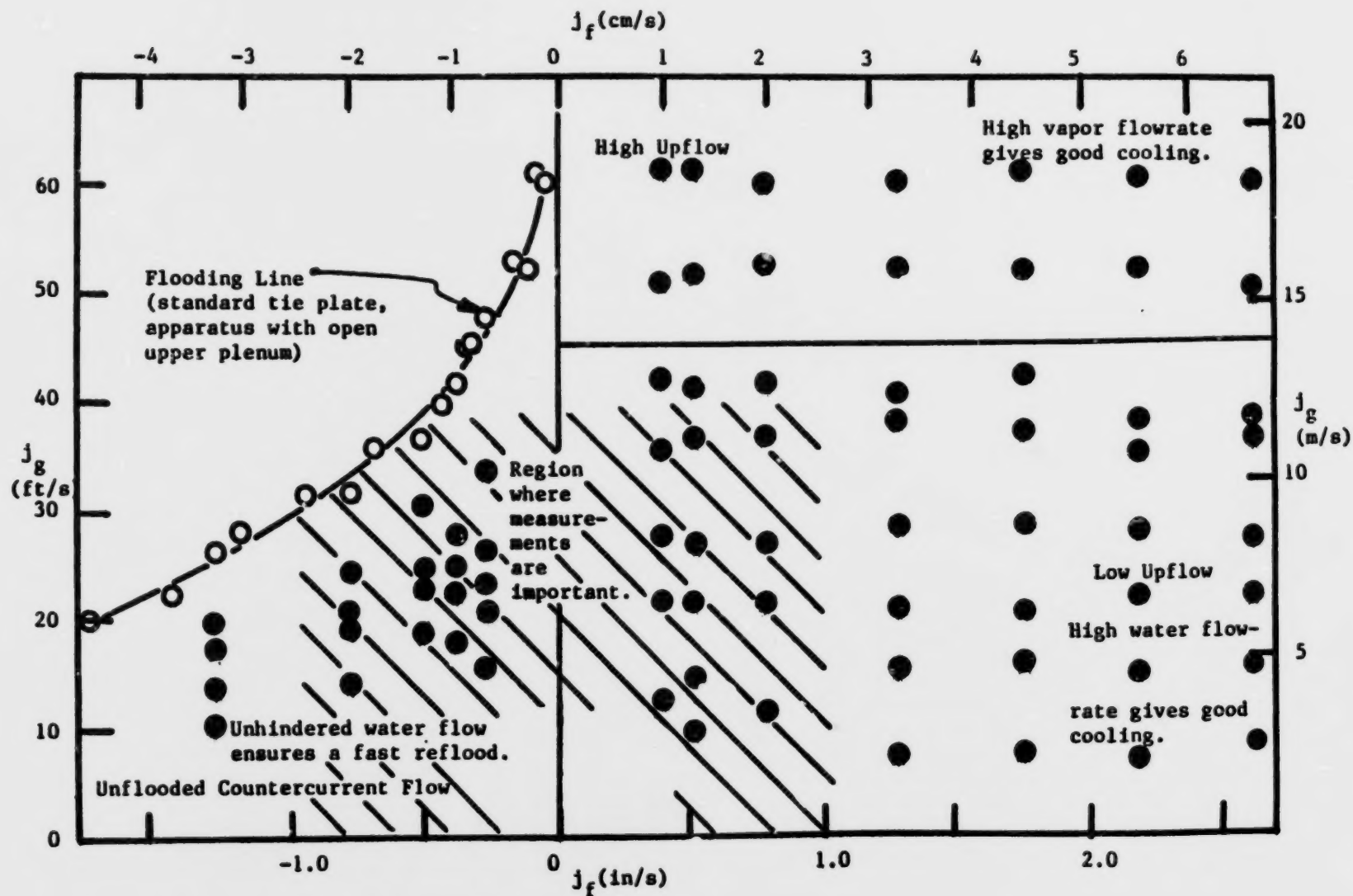


Figure 1. Data Points in M.I.T.'s Study.

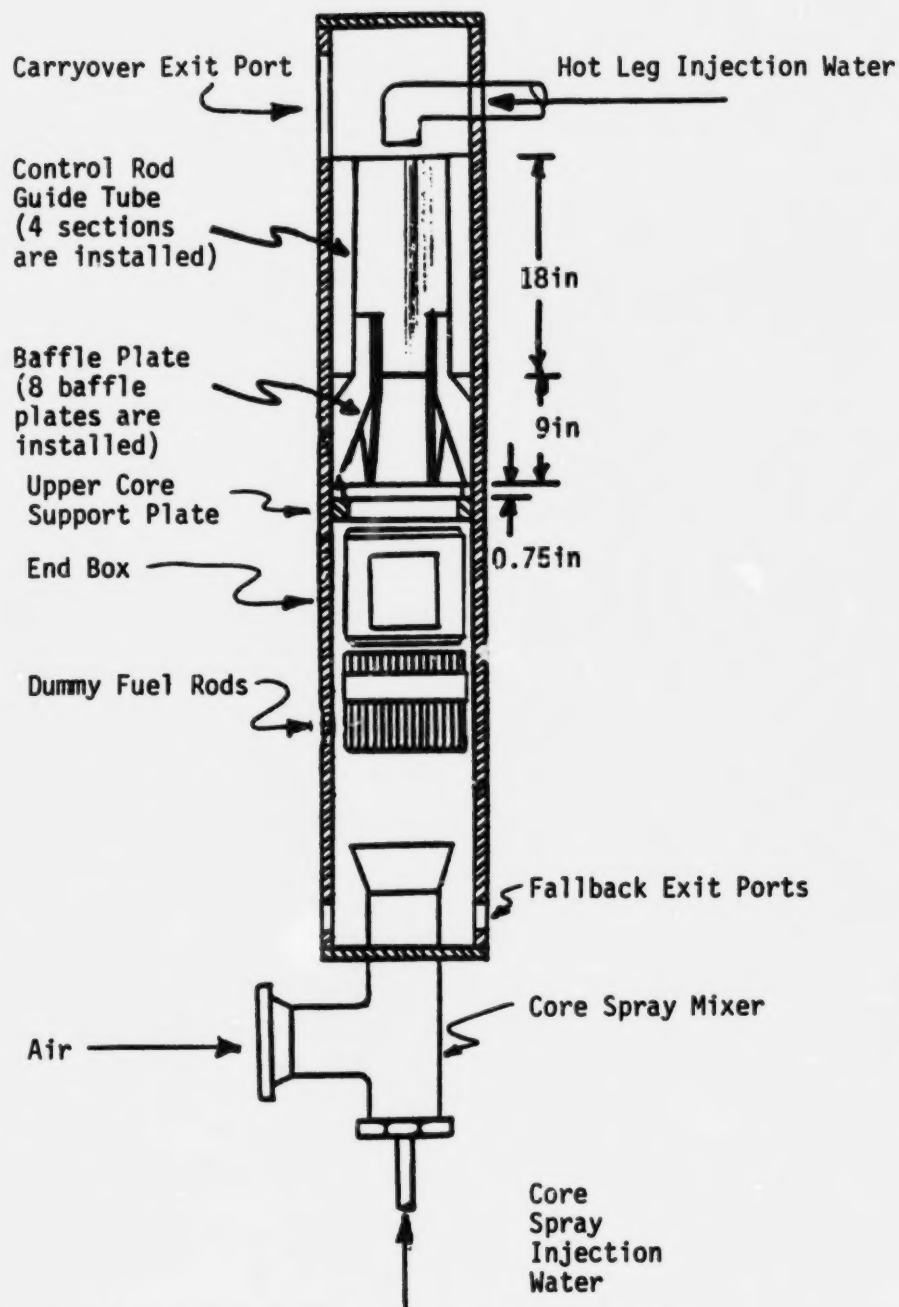
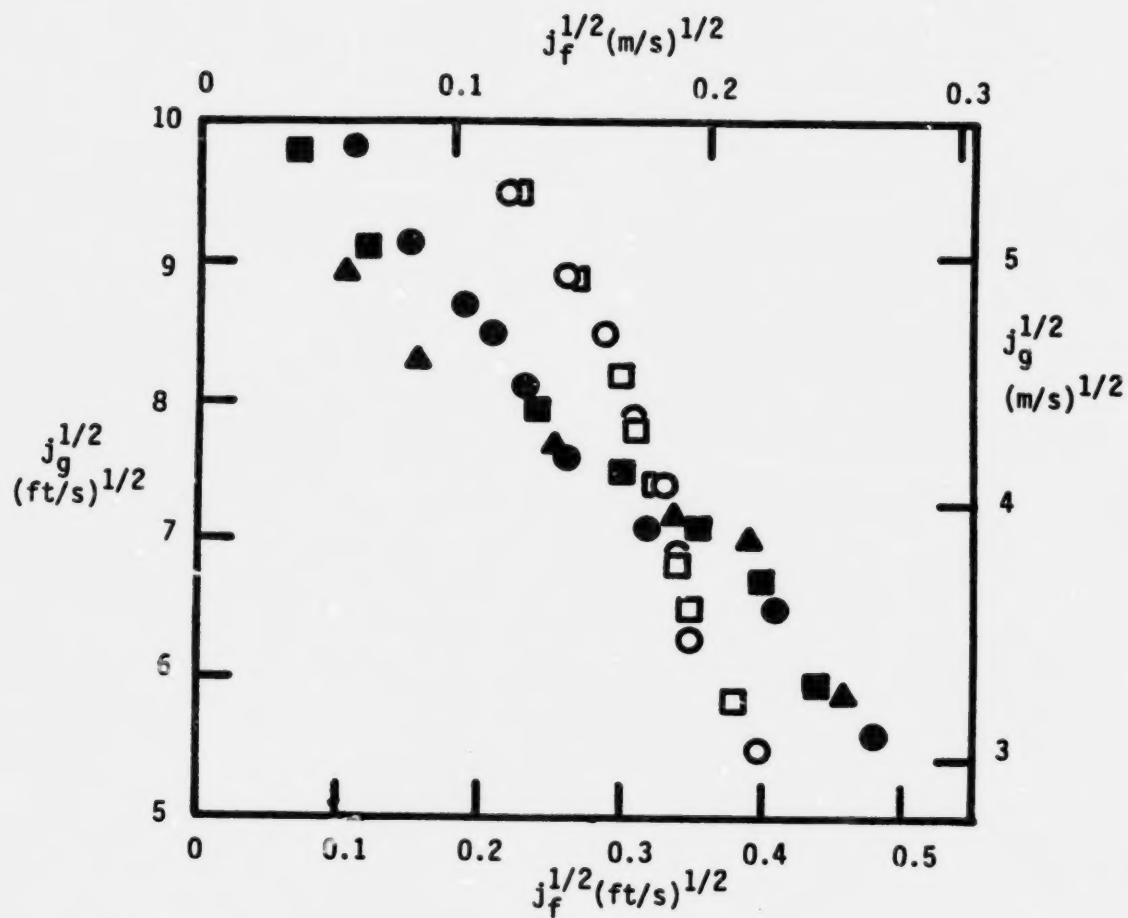


Figure 2. M.I.T.'s Apparatus with Upper Plenum Internals.

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Apparatus with	○	Injection Flowrate=19.2gpm
Simulated Internals	□	27.5
Apparatus with	●	19.2
Empty Upper Plenum	■	27.5
Blocked-Slots Tests	▲	19.2

Figure 3. Flooding Lines.

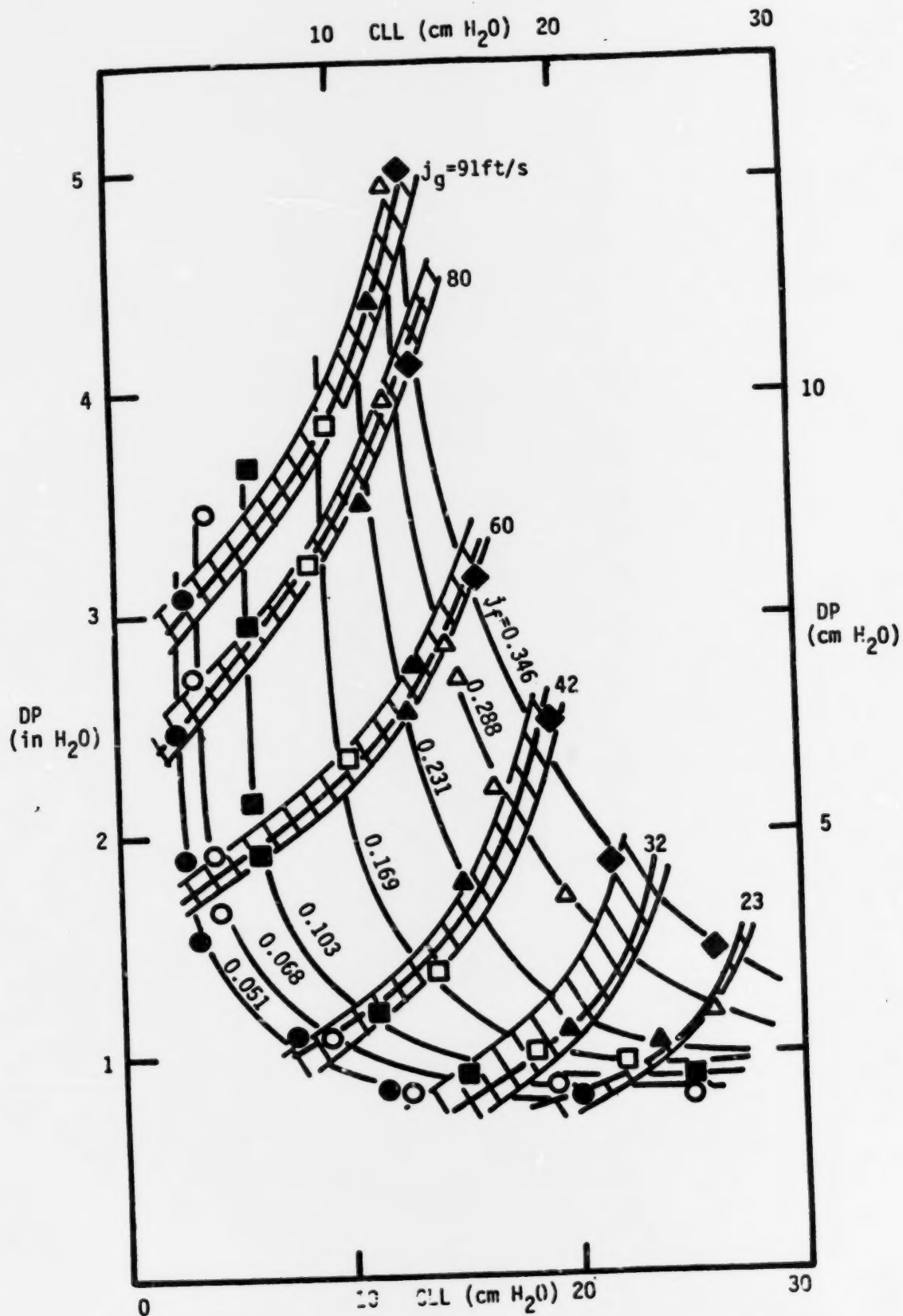


Figure 4. Upflow Algorithm

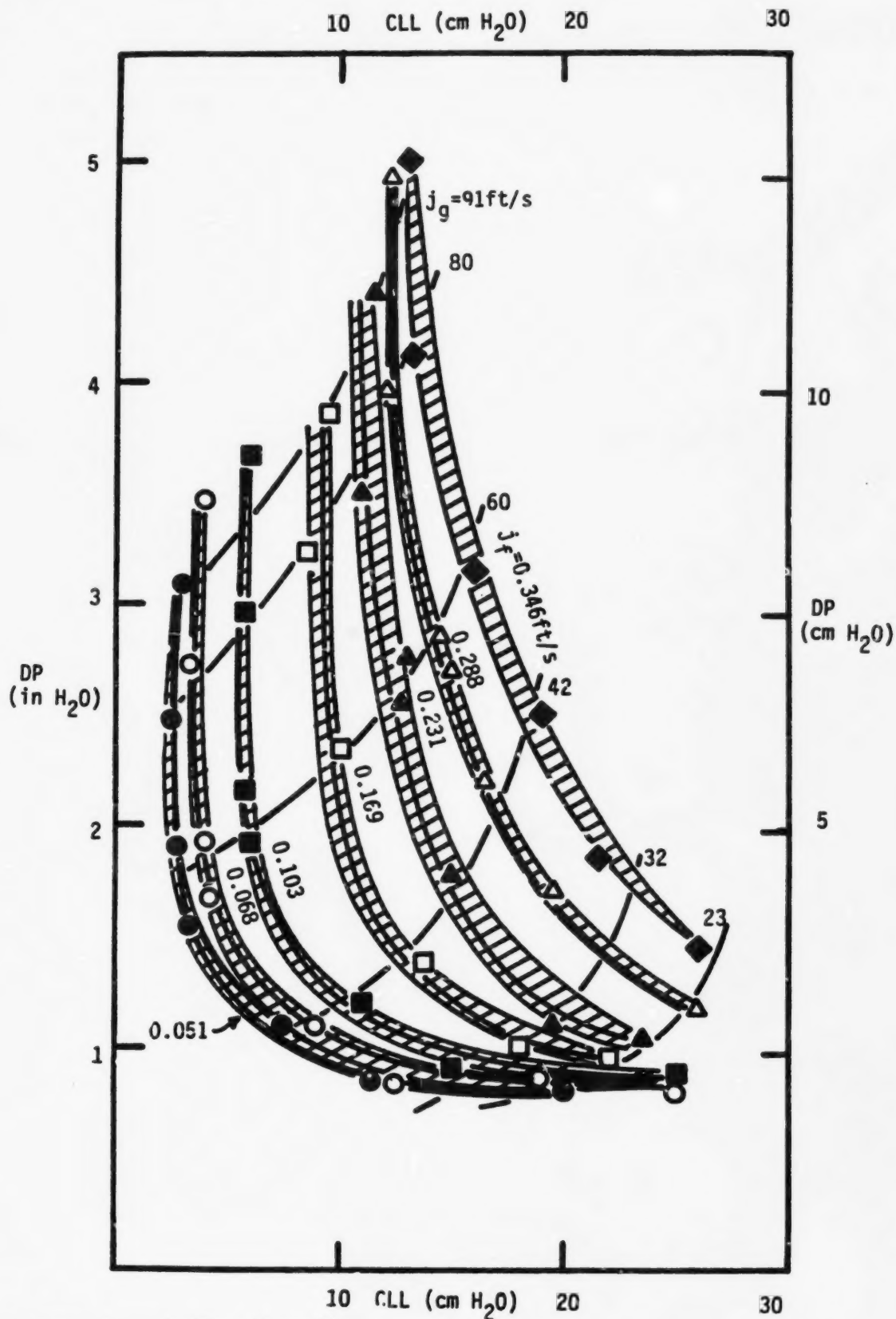


Figure 5. Upflow Algorithm.

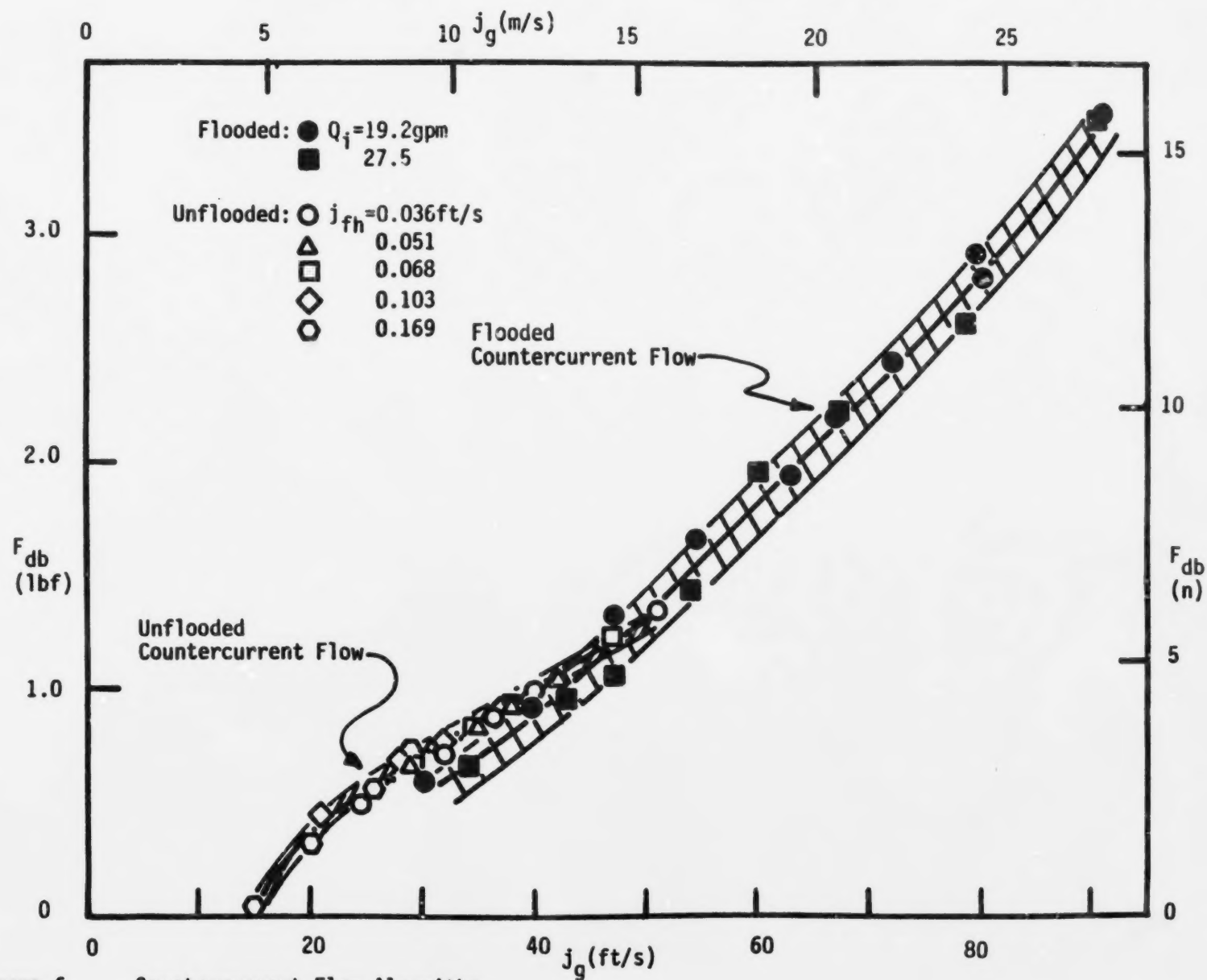


Figure 6. Countercurrent Flow Algorithm.

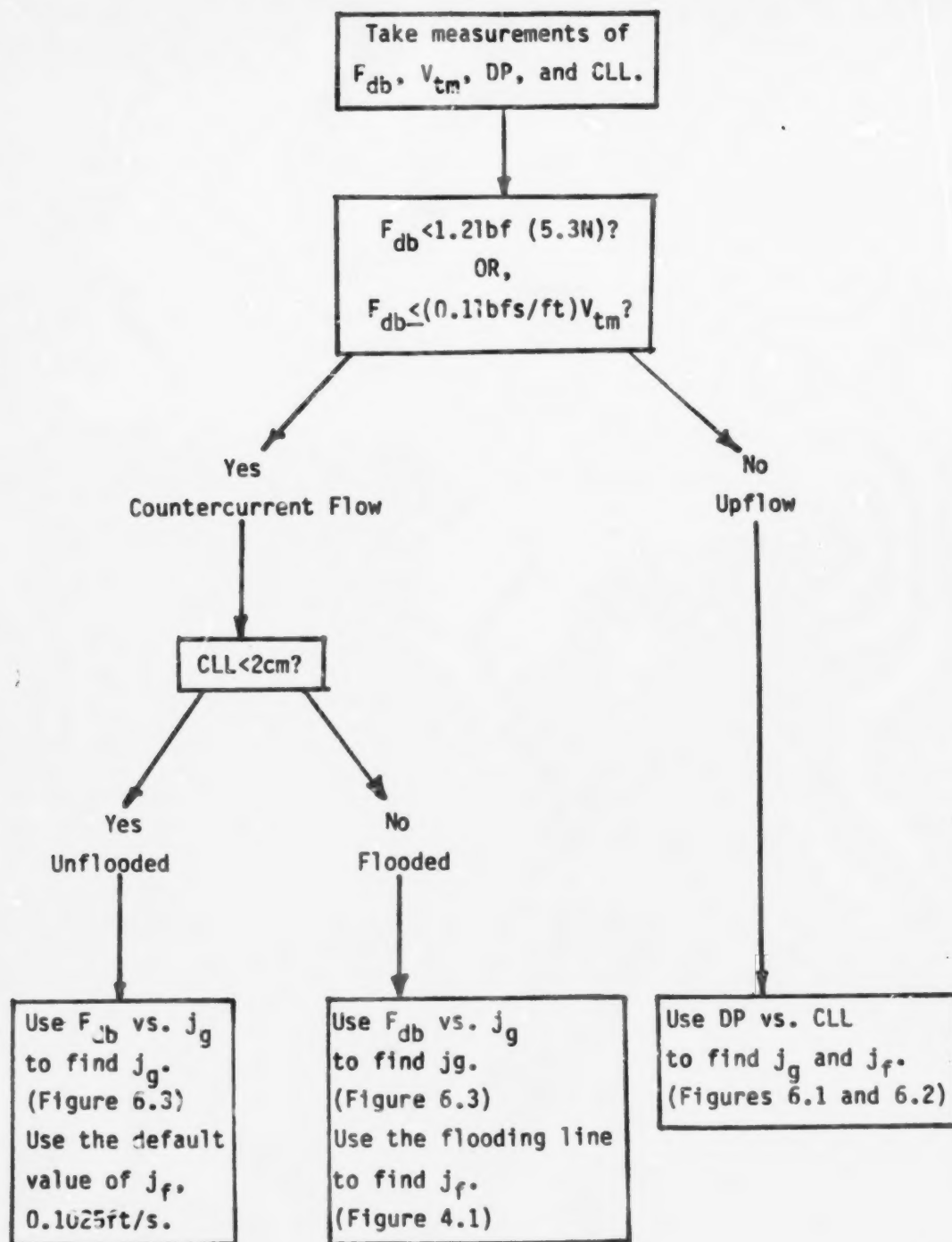


Figure 7. Summary of the Algorithm.

DRYOUT FRONT MODELING FOR ROD BUNDLES

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Abstract - The dryout zone is that region between the calculated pool liquid level and the point where a rod or tube submerged in the pool dries out. A method of calculating the extent of the dryout zone is presented. Recommended values for the drift flux model constants are given for rod bundles. These are needed to make these calculations.

For void fractions less than 0.45 at the free surface, the dryout front and the water are level virtually the same. For void fractions above, 0.45, the extent of the dryout front is proportioned to the superficial velocity of the vapor, rising to a value of .3m for a superficial velocity of 2m/s.

Introduction - During some transients in tubes, or outside rods or outside tubes in bundles, a distinct liquid level is observed. Under these circumstances, the liquid velocity is usually very low, less than 0.1 m/sec and the vapor velocity is of the order of one or two meters per second. When calculating the net heat transfer or the maximum rod or tube temperature it is necessary to know where the rod or tube dries out, as very little heat is transferred above the dryout level. This paper presents a method of calculating the location of the dryout front in a rod or tube bundle.

If the best values which we have for the drift flux model constants are used to calculate the liquid level one finds, experimentally, that there is a region above the calculated liquid level which is still wet. This region is called the dryout zone. This region is kept wet by the fluctuations in the liquid level which occur naturally in the slug and churn-turbulent flow regimes. Spray from bubbles breaking at the surface can also serve to keep the surface above the average water level wet. Figure 1 shows this flow regime, as it might be observed in a tube, along with the calculated liquid level, the fluctuation range and the observed dryout level. The discrepancy between the liquid level and the dryout front arises from the fact that the drift flux model constants are derived from average void data while the dryout front is determined from axial temperature distribution data so they need not be identical.

In an earlier work reported in reference 1, the drift flux model was used to calculate the wet region in tubes. The fit of this model was not as good as it could be for large heat fluxes or large pool depths because the contribution of the dryout zone to the effective heat transfer area of the tubes is neglected. A model which uses the dryout zone to choose drift flux model constants is unsatisfying, however, because the drift flux model constants that give the best correlation with the temperature distribution data cannot also give the best correlation for the void distribution. In fact, the drift flux model constants which are based on the time average

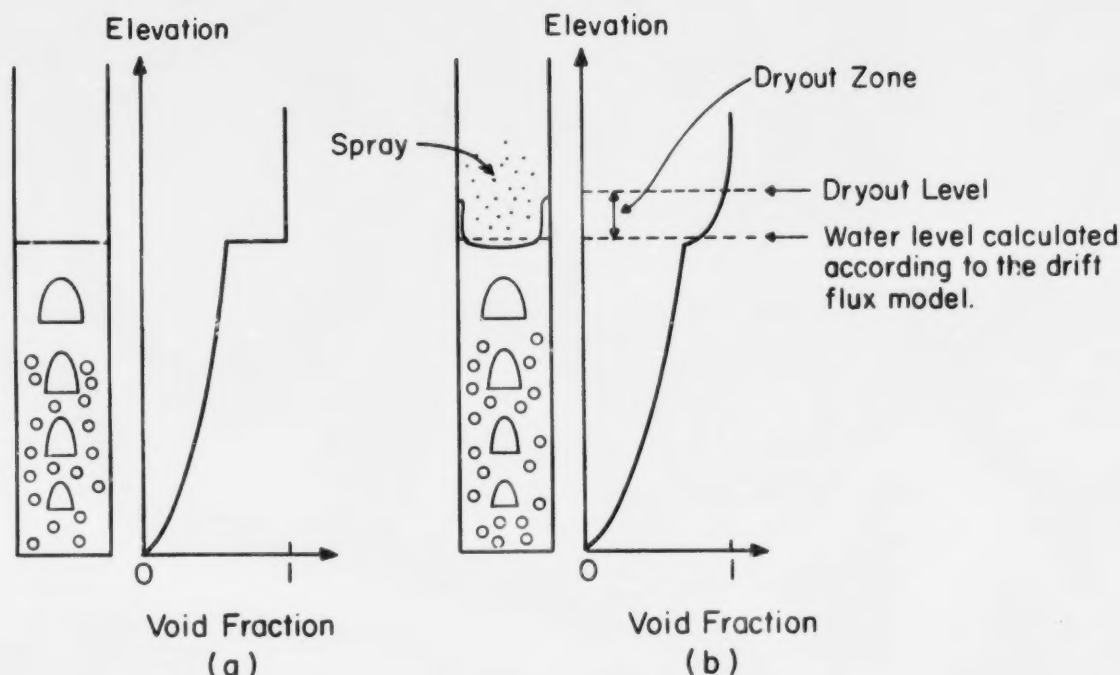


Figure (1a) - Void fraction distribution as calculated using the drift flux model. (b) Void fraction distribution showing the dryout zone, the dryout level and the fluctuation range as observed in a tube.

value of the void fraction cannot be expected to predict the level fluctuations and the carryover which are responsible for the existence of the dryout zone. It is necessary to have a separate dryout zone model.

In this paper, we take temperature data obtained in three separate experiments (2,3,4) and develop a simple criterion which can be used to predict the extent of the dryout front from the vapor and liquid superficial velocities at the free surface.

First we shall describe the experiments reported in reference (2) and then combine the results of these experiments with the results of references (3) and (4) into a consolidated dryout zone model. These data include a modest range of rod bundle geometries and considerable range of pressure in steam and water. These data also include both blow-down and steady state experiments.

Experiments - The set of experiments which we would like to describe are reported in detail in reference (2). The test section consisted of a 3x3 electrically heated rod bundle as illustrated schematically, in Figure 2. The dimensions are given in Table 1. Every .45 m there was a spacer which was placed on alternate sides. The spacer is as illustrated in Figure 3. They did not appear to alter the void fraction profile

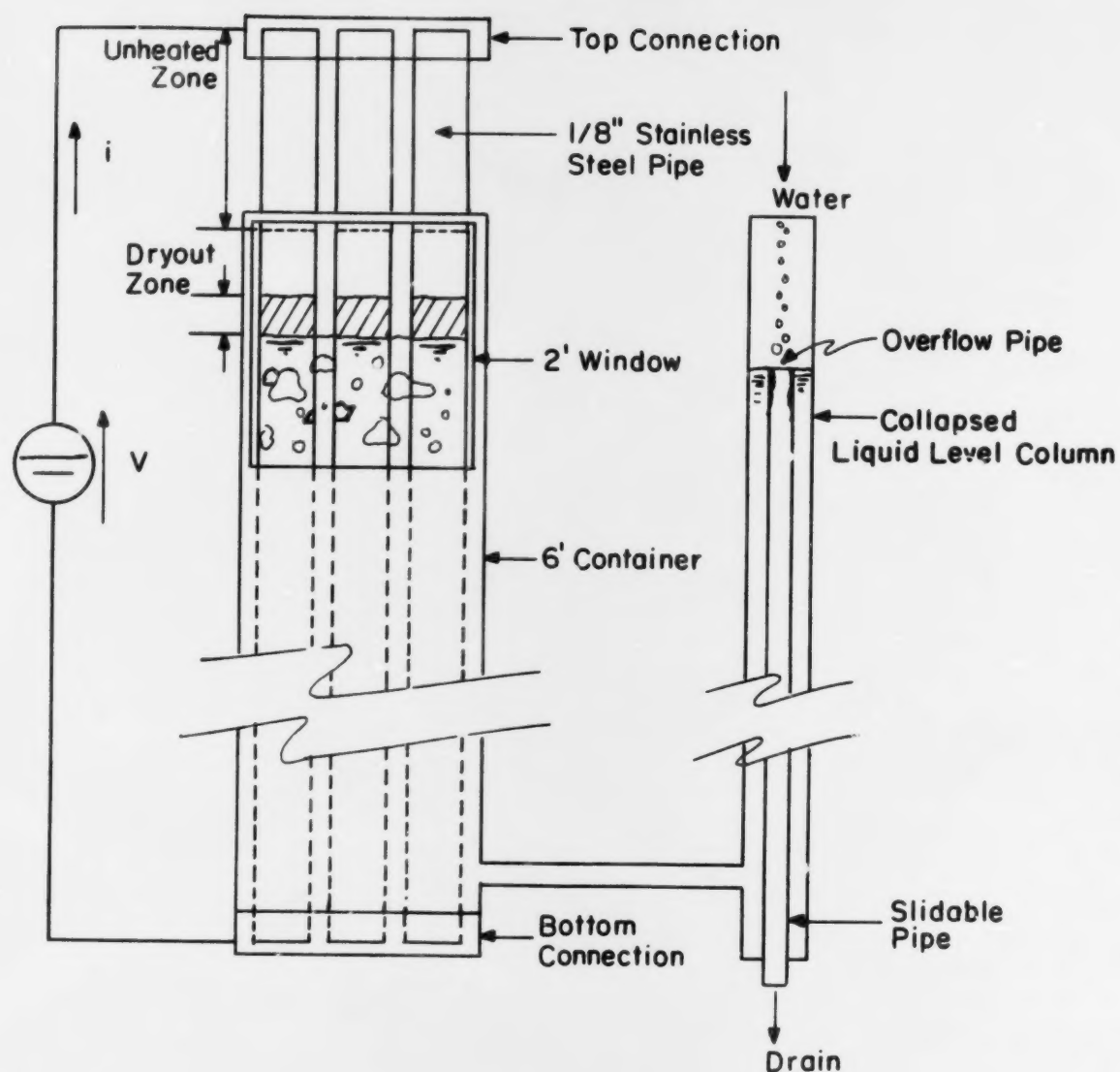


Figure (2) - Schematic of the 3 x 3 rod bundle reported in Reference (2).

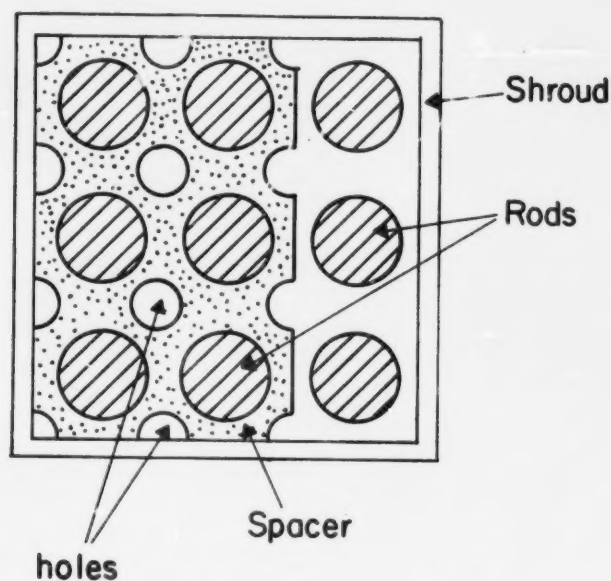


Figure (3) - Spacer geometry. These spacers were placed on alternate sides of the 3 x 3 rod bundle at .45 intervals.

appreciably. The collapsed liquid level was maintained constant by an overflow, the elevation for which could be varied. The elevation of the overflow defined the collapsed liquid level. In this way, the dryout zone could be confined to a short section near the top of the test section so that overheating the rods was not a problem.

Though we thought it would be necessary to use temperature readings to determine the extent of the dryout zone, this was not necessary, the dryout zone was easily identified by looking through the window on one side of the test section.

A typical set of data is shown on Figure 4. The correlation of this data and that of the two other sources will be presented shortly.

Calculation of the Liquid Level - The drift flux model allows one to calculate the local void fraction. The equation is shown as equation (1).

$$\alpha = \frac{j}{C_0(j_f + j_g)^g + (V_{gj})^K} \quad (1)$$

For rod bundles of the dimensions shown in Table 1, the C_0 is only dependent on the geometry of the system. Physically, it is the C_0 ratio of the maximum to the average velocity in the test section. It is dimensionless.

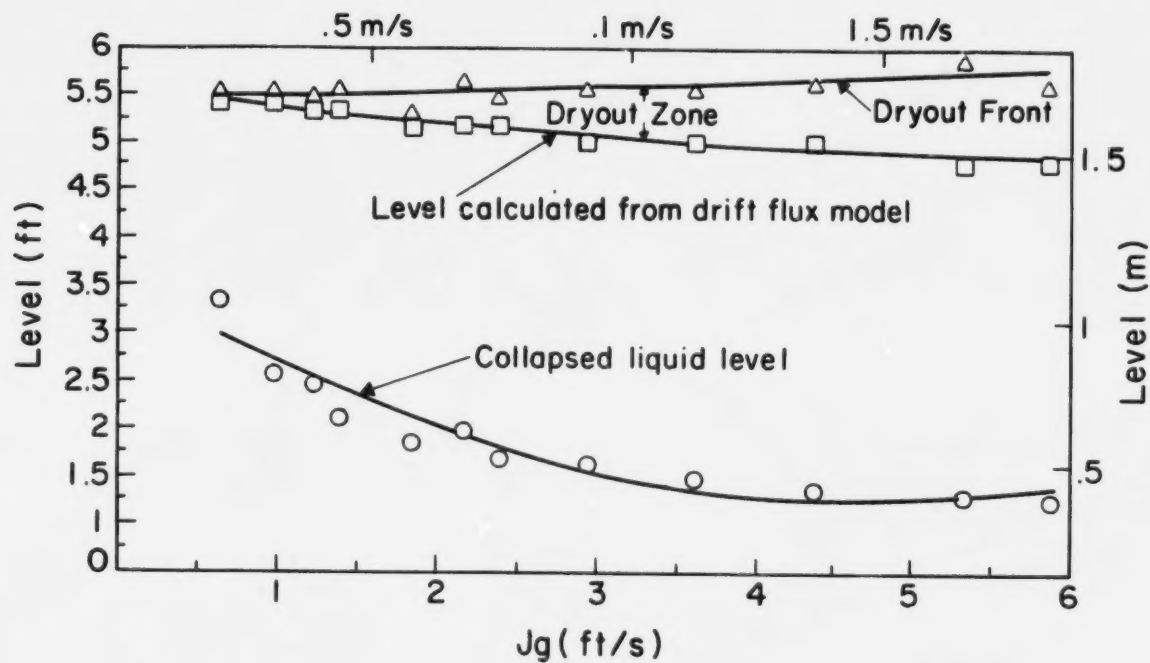


Figure (4) - Data from reference 4 showing the collapsed liquid level, the liquid level as calculated from the drift flux model and the dryout front.

Table I - Bundle Geometry

This Work	3 x 3 OD - 1.14 cm Pitch - 1.37 cm Square Array 5.08 cm square shroud
Reference 3	2 x 6 (+ 2 instrument stalks) OD - 1.6 cm Pitch - 2.4 cm Square Array 9.7 cm circular shroud
Reference 4	8 x 8 OD - .94 cm Pitch - 1.27 cm Square Array 10.4 cm square shroud

The V_{gj} is the drift velocity of the bubbles. This is the amount that the bubbles move faster than the liquid ahead of them. In reference (5) it was found that this value should be augmented by factor, K, equal to 1.6 if the test section was heated. This factor K, represents the increase in bubble velocity due to bubble agglomeration because the bubbles never achieve their fully developed spacing in a heated section. It is dimensionless. V_{gj} has units of velocity. Table 2, shows the actual values of these constants used for calculating the void fractions presented in this paper.

Table II - Drift Flux model constants and ranges of parameters tested

Reference	
This Work	P = 1 atmosphere C = 1.19 V_{gj}^0 = 20.1 cm/s K_{gj} 1.6
Reference 3	P = 33 to 3 atmospheres C = 1.16 V_{gj}^0 = 16.7 to 19.8 cm/s K_{gj} 1.0
Reference 4	P = 80 to 36 atmospheres C = 1.20 V_{gj}^0 = 18.5 to 19.1 cm/s K_{gj} 1.6

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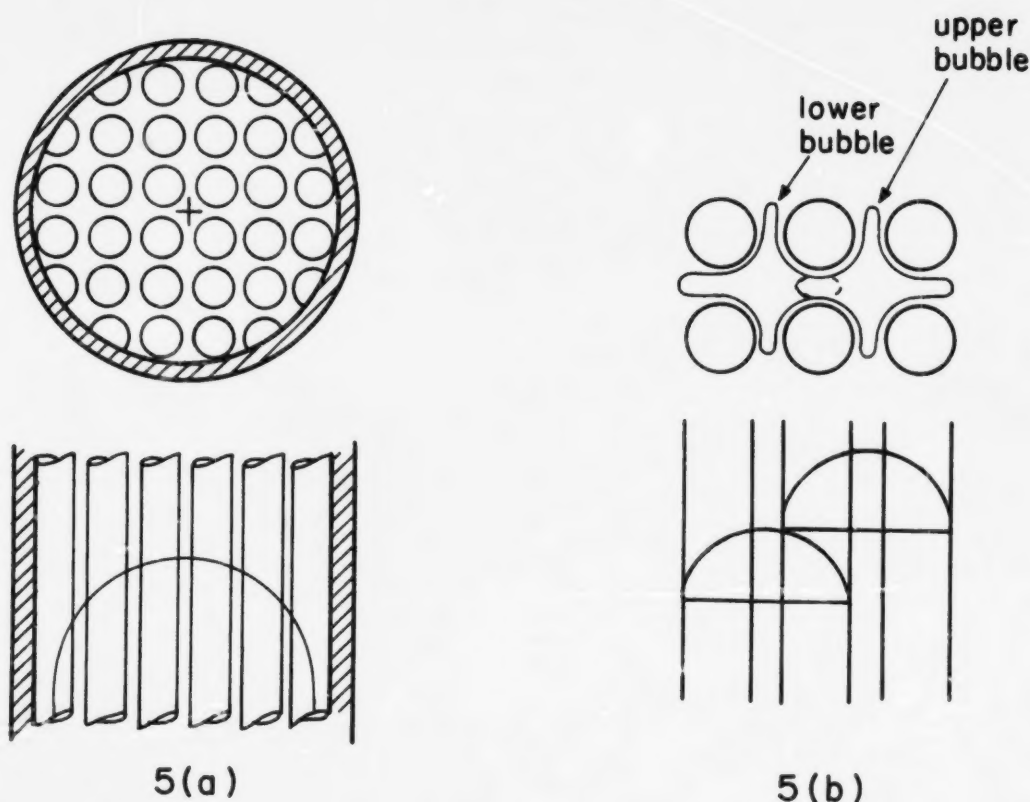


Figure (5a) - The shape of a bubble in rod bundle as determined in the tube emptying experiment (5b) to shape of the bubbles formed by heat addition in a rod bundle.

The quantities j_f and j_g are the superficial velocities of the liquid and vapor respectively. They have units of velocity.

Reference (5) reports values for V_{gj} in bundles. These were not used in this paper because they are not appropriate. At the time the measurements reported in (5) were taken, the values looked high but they were what was observed. As a result of the visual observations made in the course of this work and work completed elsewhere, (6), it is now possible to account for the uncommonly high values for V_{gj} reported in reference (5).

The experiments of reference (5) were performed by emptying a shrouded rod bundle, which was capped, and observing how rapidly the bubble rose. The bubbles observed in reference (5) looked like those shown in Figure (5a). The tube emptying experiment gives a single large bubble which rises in the shroud at a velocity reflecting, more than anything else, the shroud diameter. In a heated bundle on the other hand, the bubbles are formed and confined to the open spaces between the rods. The characteristic dimension of these bubbles reflects, more than anything else, the pitch of the rods (6). See Figure (5b). These bubbles typically, are confined to the cell

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defined by four adjoining rods. There is little tendency in either the slug or churn-turbulent regimes for the bubbles to agglomerate by migration from subchannel to subchannel. The figures adapted here as Figures (5a) and (5b) from reference (6) show this distinction quite clearly. The visual observations reported in reference (2) support the regime observations illustrated in Figure (5b).

In this work we used the drift flux model constants typical of the flow regime illustrated in Figure (5b). Because there isn't much variation in the rod bundle dimensions, the drift flux model constants are about the same in Table II.

Other Data - Before presenting the recommended correlation, the other sources of data will be described. Reference (3) reports some blowdown experiments in a six rod bundle (plus two instrument stalks). The apparatus is described in reference (7). Water was held up on simulated tube support plates while the blowdown provided the steam flow rate. The K of equation 1 was chosen to be one because there was little heat addition. The local collapsed liquid level was measured while the calculated level was determined using the constants given in Table 2. The dryout front location, as a function of time, was measured with thermocouples. The instantaneous, local values of the dryout front are what appear in Figures (6) and (7).

The data of reference (4) is our other set. These data were taken on a 64 rod bundle at a range of high pressures. A gradual blowdown occurred during the experiment but it is essentially a steady state experiment with most of the vapor provided by the heat flux from the rods. The dryout point, again, was determined with thermocouples.

With the data of reference (2), (3) and (4), the liquid level was calculated using the constants of Table 2. The void equation, (1), was integrated to determine liquid level and this subtracted from the measured dryout front elevation to determine the height of the dryout zone.

The dryout zone thickness reading was then plotted versus all sorts of parameters to see what variables were most important and the results shown in Figures (6) and (7) emerged.

Discussion - Figure (6) shows that the liquid level and dryout front in rod bundles are identical if the local void is less than 0.45. Figure (7) shows that the extent of the dryout zone is proportional to the superficial velocity of the vapor for void fractions above that.

Apparently the bubbles are so small where the void fraction is less than 0.45 that there is practically no fluctuation in the liquid level and the liquid level and dryout front are identical. At higher void fractions the liquid level-fluctuates appreciably and two quantities can differ by as much as 0.3 m. This regime is usually churn-turbulent and is accompanied by large fluctuations in level and density.

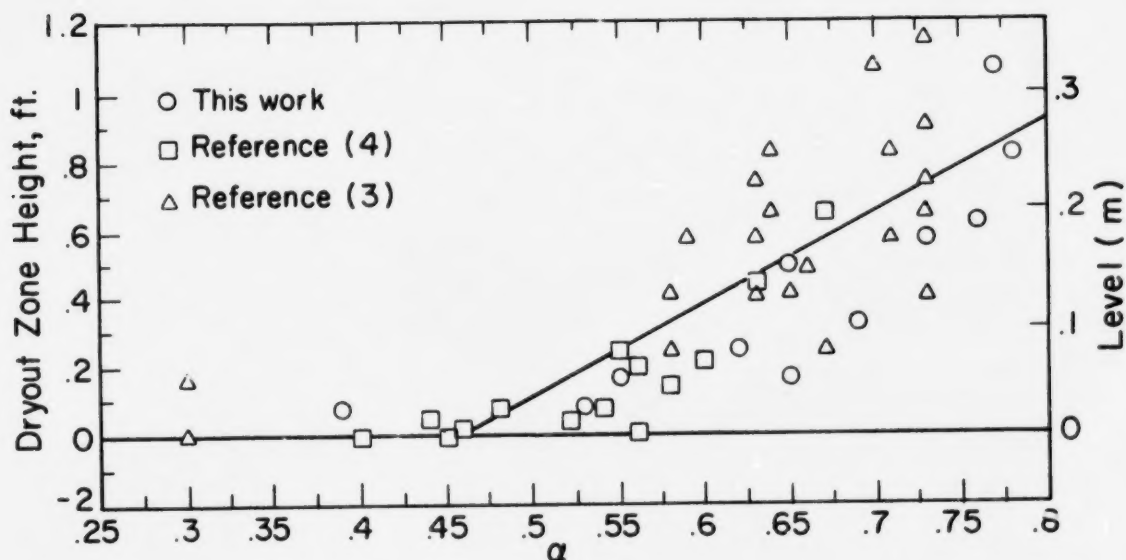


Figure (6) - The effect of void fraction near the surface on the dryout zone height. The dryout zone is essentially of zero thickness for void fractions less than .45.

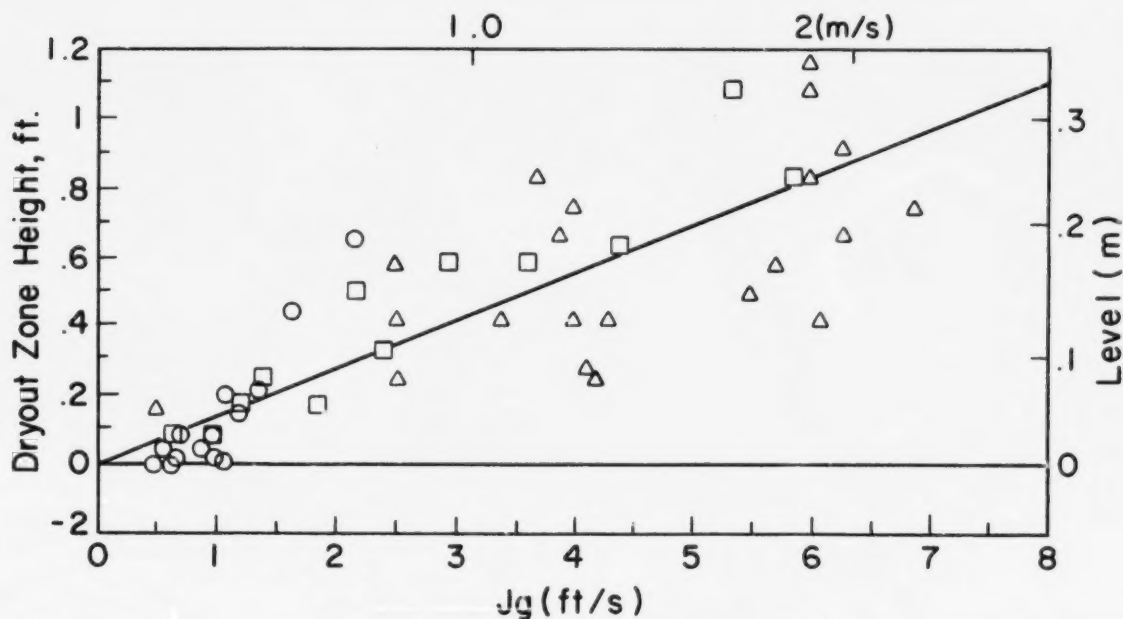


Figure (7) - The effect of the vapor superficial velocity on the dryout zone thickness. The dryout zone thickness is proportional to the superficial velocity of the vapor as long as the void is greater than .45.

Conclusions

1. For rod bundles the calculated liquid levels and the dryout front are virtually identical when the local void fraction is less than 0.45.
2. The dryout front and calculated liquid level differ by as much as 0.3 m for a superficial velocity of 2 m/s through essentially stagnant liquid.
3. For rod bundles in which the bubbles are formed by heat transfer or flashing the drift velocity is scaled by the rod pitch more than anything else. Recommended values for the constants appearing in equation (1) for rod bundle like those listed in Table 1 are:

$$\begin{aligned}C &= 1.18 \\V^0 &= 19 \text{ cm/s} \\K_{gj} &= 1.6 \text{ (heated)} \\K &= 1.0 \text{ (unheated)}\end{aligned}$$

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References

- (1) Sun, K.H., R.B. Duffey, C. Lin, "A Nondimensional Analysis of Boiling Dry a Vertical Channel with a Uniform Heat Flux" Trans. ASME, Journal of Heat Transfer, vol. 103, p 667, November 1981.
- (2) Mohamed, Jorge Alejandro, "Transient and Steady State Dryout Zone Modeling for Pressurized Water Reactors", MS Thesis in Nuclear Engineering, MIT, August, 1986.
- (3) Brown, David and P. Griffith "Dryout Front Modeling for PWR Thermal Hydraulic Analysis" NUREG/CR-4581, May 1986.
- (4) Hyman, C.R., Anklaam, T.M and White, M.D., "Experimental Investigations of a Bundle Boil-off and Reflood under High Pressure, Low Heat Flux Conditions", NUREG/CR 2455, ORNL 5846, 1982. See Runs 3.09.10T, 3.09.10V and 3.09.10X.
- (5) Griffith, P., "The Prediction of Low Quality Boiling Voids" , Trans ASME, Journal of Heat Transfer Vol. 83, p 327.
- (6) Dukler, A.E., Taitel, Y., "Flow Patterns in Gas-Liquid Systems: Measurements and Modeling", Multi-phase Science and Technology, Edited by Hewitt, Delhay and Zuber, Hemisphere Publishing Co., 1986. Vol. 2, p 80.

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Steam Separator Modeling for Various Nuclear Reactor Accidents

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Christoph Knoess, Visiting Engineer
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ABSTRACT

This is a progress report on the MIT program on separator modeling. Experiments were performed using air and water on three types of centrifugal separators plus gravity and secondary separators. Experiments were also done in the MIT blowdown rig, with and without a centrifugal separator, using steam and water. Appreciable carry-over from the steam generator occurs when the drain lines from the three stages of separation (centrifugal, gravity, and secondary) are unable to carry-off the liquid flow due to the high downcomer water level. Failure scenarios of the separator for extreme range of conditions from the quasi-steady-state transients to the fast transients are presented. A module showing the general separator model structure is provided and recommendations are made how to model the separator.

1. INTRODUCTION

In a Pressurized Water Reactor (PWR) steam generator, a moisture separator is used to separate steam and liquid and to insure that essentially dry steam is supplied to the turbine. Most separator system consist of three separators known as primary, gravity, and secondary separators. The primary separator is a centrifugal separator which uses centrifugal force to separate water from steam. The secondary separator (steam dryer) is constructed from corrugated or hooked vane chevrons, and separation is achieved by impingement of droplets on these plates. Under normal conditions when operating within the design envelope, this series of separators essentially provides practically 100% dry steam. During a steam line break or combined steam line break plus tube rupture, however, the behavior of the separator is unknown. These accidents result in two kinds of transients: i) quasi-steady-state and ii) fast. For a sufficiently small steam line break or a tube rupture with pressure relief valve stuck open, a transient can be simulated as quasi-steady-state. In this case, the flow rate of steam and water through the separator is low enough so that system operates within the design envelope. Only downcomer water level may be high enough to be outside of the design envelope. However, a large steam line break or steam line break combined with tube rupture could result in a fast transient. When the large break occurs, the flow rate through the separator could be several times of design value, the system pressure drops, flashing occurs from all solid surfaces as well as within the body of water,

and the downcomer water level swells. All these factors which have no counterparts during normal operation could affect the performance of separator.

How the separator will perform under these circumstances is very important in two aspects: iodine carry-over and the water inventory in the secondary side. A steam line break combined with a tube rupture or a tube rupture with pressure relief valve open provides a direct leakage path through and out of the secondary system for radioactive materials contained in the primary fluid. Under this condition, where almost all the radiation released is in the form of iodine, the amount of liquid (and thus iodine) released to the atmosphere depends very strongly on how the separator performs. Also, how the separator will perform under the steam line break conditions is very important because it can substantially change the water inventory in the secondary system. This is the principal factor that affects the heat transfer from primary to secondary side. When the break occurs, the pressure in the secondary side drops and the heat transfer rate from the primary to the secondary side increases. The excess heat transfer results in an overcooled primary system. Moreover, the reactor trip following a steam generator blowdown will enhance the effect of overcooling. As a result of this overcooling in the primary side, the emergency core cooling may come on and lead to repressurization thereby threatening pressurized thermal shock. Thus, the heat transfer on the secondary side is important because it governs the degree and duration of the overcooling on the secondary system. As long as the tubes in the secondary side are wet, the heat transfer is excellent. On the other hand, whenever the tubes are dry, the heat transfer is negligible. Thus, the fraction of wetted area which depends on water inventory is particularly important. The performance of the separator is one of the key parameters for determining the water inventory.

Previous to this work, there was no available data and also no means of predicting the separator performance under conditions outside of design envelope. The computer system codes such as TRAC or RELAP either don't have any separator model or have a model based on assumption of 100% separator efficiency which is not true in real transients. Therefore, there is a general need for a separator model which can improve overall answers in transients. Because of the complexity of the two-phase vortex flow around the separator, it is very difficult to model it analytically as a two-phase three dimensional flow or possibly two dimensional axisymmetric flow. Since those codes have a one-dimensional steam generator model, it is necessary to have a simple separator model such that it is easy to put into the code without too much difficulty. The goal of this project is not only to provide separator modeling instructions but also to develop a simple separator model.

2. EXPERIMENTS AND RESULTS

2.1 Purpose of Experiments

The separators used in PWR steam generators differ widely in geometry and have their own unique design features. The specific carry-over performance also varies depending upon the type and design of each separator. However, all show adequate performance within the design envelope. It has been shown that

the performance of the separator can be affected by flow loading and downcomer water level. Most separators were not tested in conditions which can occur in transients such as a tube rupture and a steam line break. These transients may give high flow rates and high downcomer water levels that are outside of the design envelope. Thus, it is necessary to do experiments to identify how the separator performs outside design conditions.

Experiments were divided into two groups: air-water steady-state experiments simulating quasi-steady-state transients and steam-water fast transients. An air-water mixture is used to simulate two-phase flow conditions for quasi-steady-state experiments because of simplicity and economics. The main objectives of the air-water steady-state experiments are: i) to provide the understanding of complex two-phase flow separation mechanism, ii) flow visualization to assist in developing a separator model, iii) to provide a data base for separator performance outside of the design envelope, iv) to identify the principal parameters and effects that characterize separator performance.

To provide maximum flow visualization, all air-water separator systems were built in with transparent models of the separators. The fast transient experiment was simulated in the MIT blowdown rig in steam-water conditions with and without the separator. It was believed that experiments performed with steam and water would give more insight into some of the physical phenomena involved in the dynamics of a steam generator and allow us to see the effect of a separator during a blowdown transient such as a steam line break.

In general, there are many types of centrifugal separators used in PWR U-tube steam generators. Because the detailed geometry of separator varies even in one type of separator by the same manufacturer, it is difficult to have one geometry to represent all separators. Thus, initially the cyclone was chosen as a generic centrifugal separator. Also, to identify the effect of geometric differences, we tested reduced scale models of the Combustion Engineering stationary vane type separator and the Westinghouse swirl vane type centrifugal separator. In the cyclone air-water system, the cyclone was connected in series to a gravity separator and a secondary separator (chevron plates) as a dryer. For the air-water system with the Westinghouse and Combustion Engineering type separators, the gravity and secondary separators were not included to isolate the effect of centrifugal separator. Thus, for quasi-steady-state air-water experiments, three different centrifugal separators were tested such that the effect of geometry on separator performance could be identified. In the fast transient experiment, a cyclone and a gravity separator were tested in blowdown situations. The experiments were performed with steam and water at an initial pressure of 800 psi.

2.2 Experiment Description and Results

2.2.1. Cyclone with Gravity and Secondary Separators

The system consists of a cyclone as the primary separator, a space above the deck plate as the gravity separator, and chevron plates as the secondary

separator. The schematic view of the apparatus with its features is shown in Figure 1. The separator system was built with Plexiglas to provide maximum visibility except for the chevron plates and the cone section of the cyclone. An air-water mixture was used as test fluid. The air was fed from a Frame 412 Roots RCS-J blower with a capacity of 1100 cfm at 4 psi pressure drop. Water was recirculated from the downcomer via the storage tank to the riser by a centrifugal pump. Also, city water as feed water was connected to the storage tank to make up for the carry-over to keep the constant downcomer water level.

The air-water mixture travels through the horizontal riser and enters the cyclone tangentially. As the two-phase mixture enters the cyclone, a vortex is generated and the water are centrifuged to outer walls where they move downward to the downcomer through the liquid drain line. The air moves to the center of the vortex and up through the gas outlet. Water which is entrained by the air is separated by the gravity and secondary separators. All tests were run in steady-state conditions. With given constant inlet air and water flow rates, the feed water was adjusted to keep the downcomer water level constant. When the inlet flow rates and the downcomer water level reached the desired steady-state value, the carry-over from the system was collected in a weigh tank and all measurements and visual observations were recorded. After a certain time period, usually 10 minutes for low carry-over, the system was turned off and the carry-over was measured. Then, the above procedures were repeated for another downcomer water level or different inlet flow rates.

In this experiment, each test series consisted of constant air and water flow rates with several downcomer water levels which were varied from the bottom of the cyclone drain line to 4 - 5 inches below the deck plate. There were 26 test series (see table 1 for the test matrix) to examine the effect of j_g (superficial gas velocity defined at the riser), j_f (superficial liquid velocity defined at the riser), the downcomer water level, and the pressure difference (ΔP) across the deck plate. In all test series, when the downcomer water level is low, the total separator efficiency is practically 100%. There is a small amount of droplet entrainment (less than 10% of inlet flow) from the cyclone and also air carry-under through the liquid drain of the cyclone when the water level is low. When the downcomer water level is about 10 - 15 inches from the bottom of drain, there is no carry-under but two distinct centrifugal hydraulic jumps (or vortex breakdown) occur at near the inlet and the bottom drain line. Further increases of the downcomer water level cause a more turbulent centrifugal hydraulic jump near the inlet such that an air core becomes small and more air with water passes from the inlet to the gas outlet pipe directly.

In Figure 2, the carry-over from the system was plotted as a function of the downcomer water level for a range of j_g and j_f equal to 1.48 ft/sec. The separator efficiency (η) and carry-over is defined as:

$$\eta = \frac{M_{w, \text{ separated (mass flow rate of separated water)}}}{M_{w, \text{ in (mass flow rate of inlet water)}}$$

$$\text{Carry-over (\%)} = (1 - \eta) \times 100$$

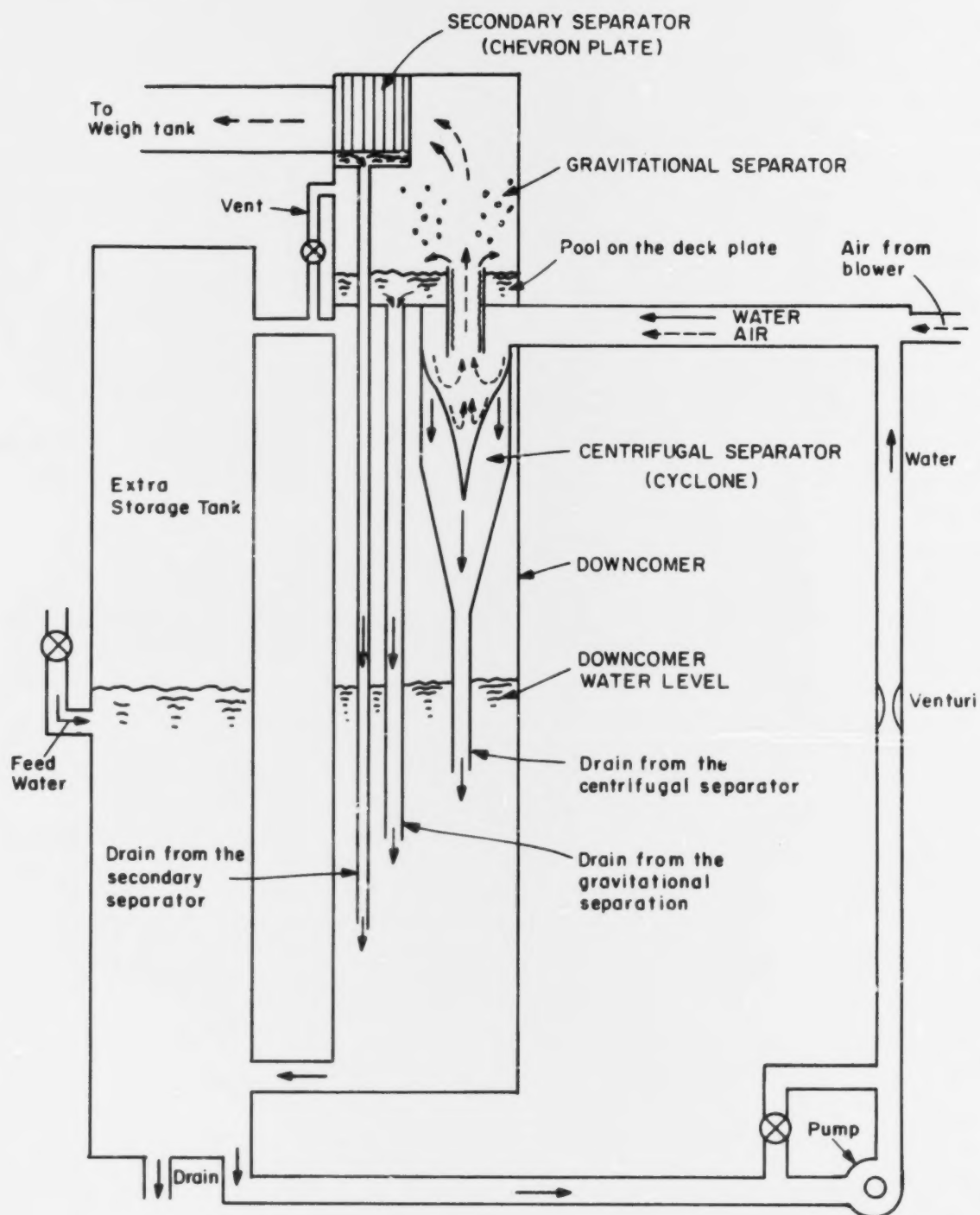


Figure 1 Air-Water Cyclone Separator System

Table 1. Test matrix for cyclone separator system.

j_g (ft/sec) \ j_f (ft/sec)	30.5	38	45	53	68.5	83	99
1.045	x	x	x	x, Δ , o	x	x, Δ	x
1.48	x	x	x	x, Δ , o	x	x, Δ	x
2.09	x	x	x	x	x	x	

- j_g and j_f are defined at the 3" horizontal riser.
- Downcomer water level was varied from 5" or 10" to 46" in most test series.
- x : The vent across the deck plate was open.
Negligible pressure drop across the deck plate.
- Δ : The vent was closed. The pressure difference across the deck plate was 10" of water.
- o : The vent was closed. The pressure difference across the deck plate was 20" of water.

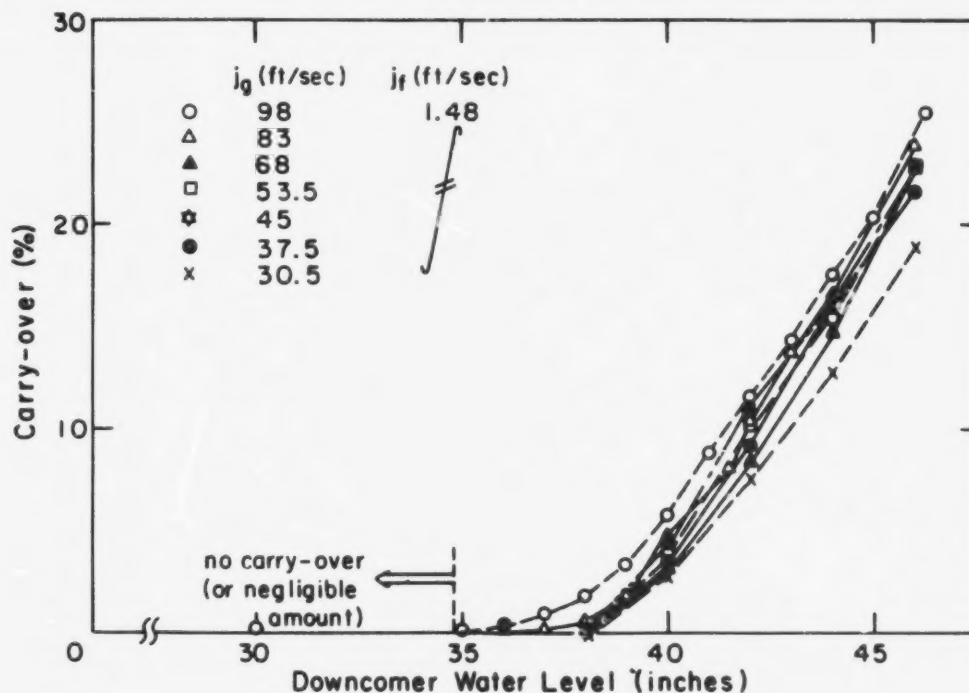


Figure 2 Overall Carry-over for a Family of Air Flow Rates in Cyclone Air-water System

The downcomer water level was defined from the bottom of the cyclone liquid drain line as a reference point. In Figure 2, the carry-over from the system starts at the water level about 35" to 38" and then increase very rapidly. The effect of j_g on carry-over is very small. Also, comparing with other test series with different j_f it can be found that the j_f effect is also minor compared to that of the downcomer water level. Figure 3 shows the carry-over from each stage of separation. There is a delay in the downcomer water level at which carry-over starts from each separation stage. When the water level goes up, the available head for the cyclone drain line is reduced so that less flow will go down and more is carried over to the deck plate. Even though there is carry-over from the cyclone, the gravity and secondary separator are able to separate up to a certain point. Further increases in the downcomer water level also cause more carry-over from both gravity and secondary separator. The performance of the gravity and the secondary separators depend on the drain sizes and capacities based on the pressure balance. In this experiment, the effect of the secondary separator is small because of the relatively small drain line. If the system has bigger drain lines for both gravity and secondary, the carry-over from those separators will be delayed until higher water levels and can be calculated without difficulty as long as the amount of carry-over from the cyclone is known.

In the cyclone, the pressure drop across the inlet to the gas outlet is important because it determines the pressure difference from the inlet to the downcomer and thus the available head for the water in the cyclone drain line. In Figure 4, the pressure drop was correlated as a function of inlet two-phase momentum. The void fraction at the inlet was calculated by the Martinelli correlation. The pressure drop model is an essential part of the cyclone efficiency model.

2.2.2 Westinghouse Turbo Vane Separator

Air and water tests were performed on a Plexiglas model of the Westinghouse type separator. The overall setup is shown in Figure 5. The air was supplied by the same source as above. Also, the water was circulated in a way similar to the cyclone experiment but with a different system. Following mixing, the air and water flow up the vertical riser (3" diameter) towards the separator depicted in Figure 5.

The separator consists of a center body and four inclined vanes. Each blade is positioned at a 37° angle with respect to the vertical axis, and spans an included angle of 112°. Due to the swirling action of the vanes any water in the center of the pipe travels to the outside. Liquid is removed from the riser by two means: the perforations on the wall downstream of the separator and the opening near the deck plate for the annular passage outside the riser. Water not escaping is carried over and travels with the air through the orifice in the deck plate. In contrast to the investigation of the cyclone described above, attention is focused on the primary centrifugal separator with no gravity or secondary separation. So, carried over water accumulates on the deck plate and drains to the collection tank to be weighed.

Air and water flow rates were monitored to determine the gas and liquid riser

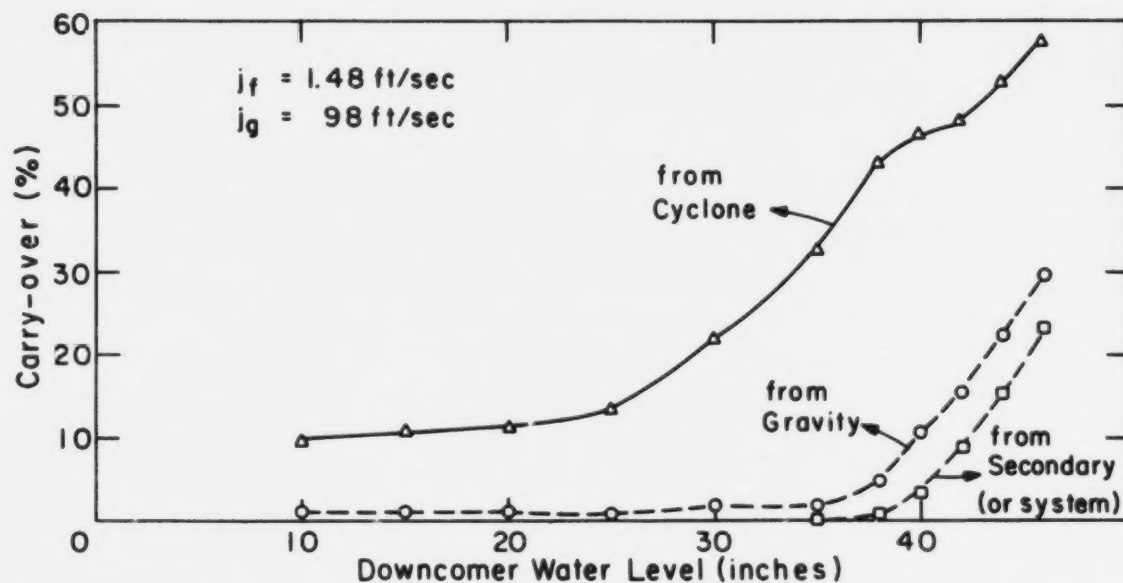


Figure 3 Carry-over from each Stage of Separation in Cyclone Air-water System

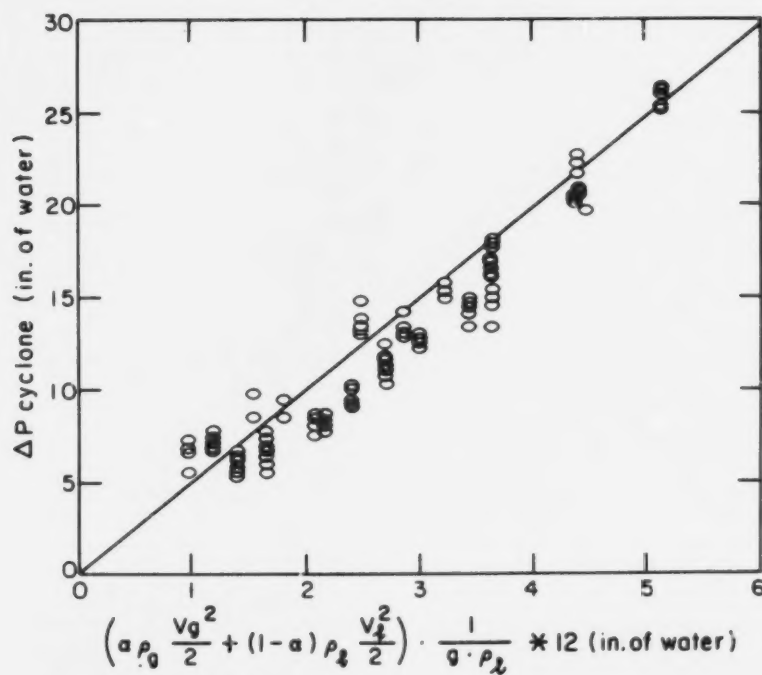


Figure 4 Pressure Drop across the Cyclone

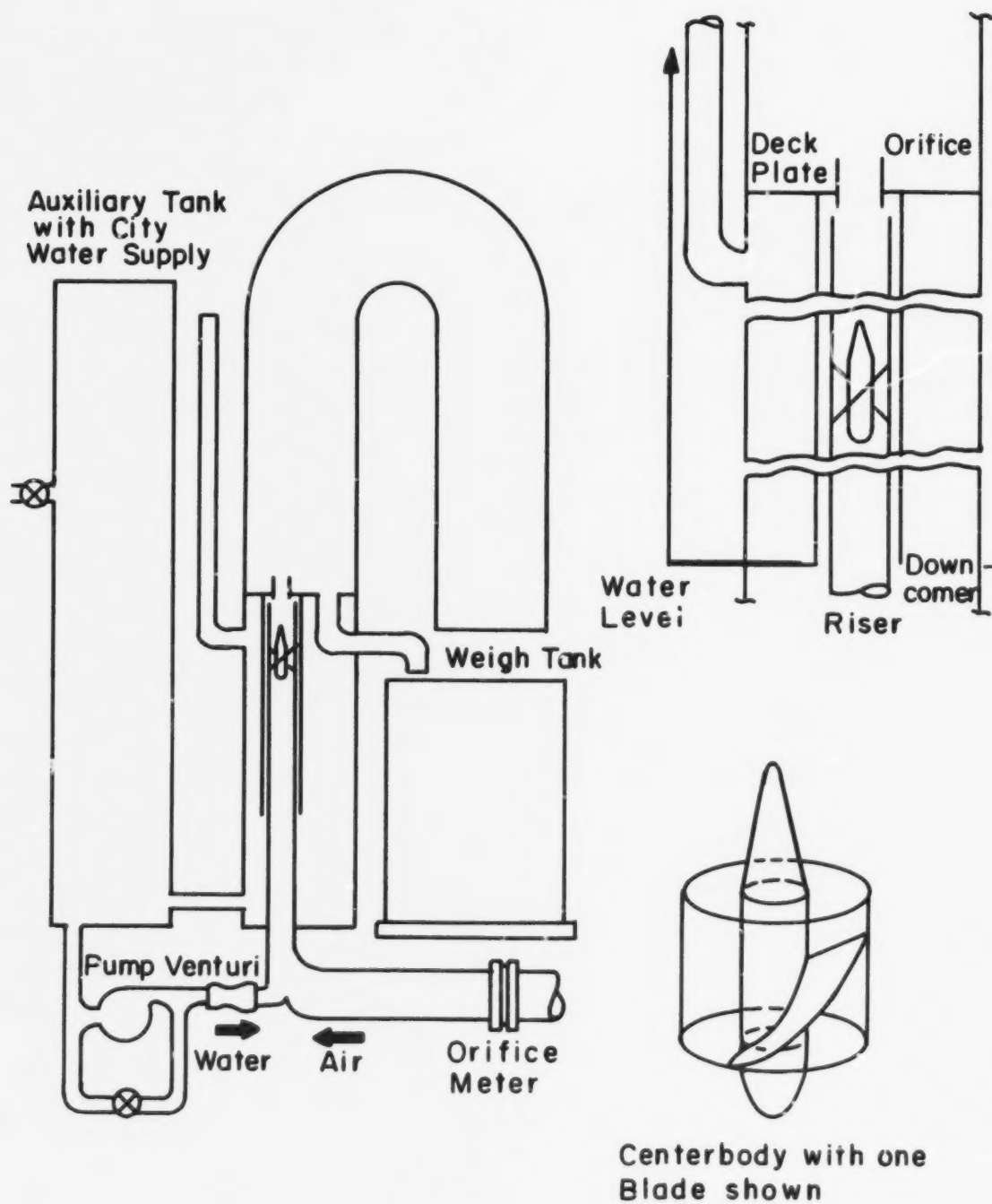


Figure 5 Westinghouse Type Separator Air-Water System

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superficial velocities (j_g and j_f respectively). The downcomer water level was controlled by adding water to make up for carry-over. A wide range of water levels which are defined as the distance from the bottom of the annular passage were examined from 0 to 60 in. in increments of 5 in. with a level of 40 in. corresponding to the deck plate. To test above the deck plate, the deck vent was closed and an external standpipe was added to raise the water level. This allowed additional hydrostatic head to be applied against the separator drain without flooding the deck plate and submerging the orifice outlet. Pressure taps provided information at several locations inside the separator. Carry-over was measured using the same method and procedures as in the cyclone tests. The scope of the steady-state tests conducted is shown in Table 2.

A typical plot of carry-over versus water level is shown in Figure 6. At low water levels, there is a relatively small amount of carry-over which a gravity and secondary separator would remove. There is also carry-under in the drain at low water levels which diminishes as the water height increases. As the water level approaches the deck plate, the carry-over increases dramatically showing water level as the dominant effect with j_f and j_g making small contributions in comparison. As in the case of the cyclone, the water level effect on carry-over can be viewed as a decrease in drain capacity of the separator.

2.2.3 Combustion Engineering Centrifugal Separator

The CE separator is a stationary swirl vane with a perforated cylinder (see Figure 7). The standard version of the CE separator has a screen assembly which also attracts a small amount of moisture. However, in this experiment, the screen assembly was not included such that only centrifugal separator effect could be seen. The separator was scaled down by the ratio 0.3125. This ratio was chosen so that it is easy to accommodate all possible flow rates and also to use a standard pipe size (as close as possible). All separator and vanes were built with Lexan and the dimensions are shown in Figure 7. The CE separator was installed in the previous cyclone separator system with minor changes. The secondary separator and the deck plate were kept to collect all the water which is carried-over from the centrifugal separator. However, unlike the cyclone system, both drain lines were connected directly to the water collection tank outside of the vessel so that the capacity of the drain lines is not affected by the downcomer water level. Thus, only the effect of the centrifugal separator can be observed.

The air-water mixture travels through a short vertical section and is redirected by the top plate to the vanes. The vanes impart a centrifugal force to the mixture. In a vortex motion, water flows up the inner wall and the air rises in the center of the separator. The water exits through the perforations in the wall, and the air flows through the exit nozzle. The carry-over from the separator is collected and measured. The procedures of the tests were same as those in the cyclone separator.

In this experiment, j_g was varied from 30 to 80 ft/sec, j_f from 0 to 4 ft/sec, and the downcomer water level from the bottom of the separator cylinder to the near top of the cylinder (see table 3 for test matrix). The carry-over vs

Table 2. Test matrix for Westinghouse separator.

H (ft/sec)	j_g (ft/sec)			
	0	30	40	50
0		x	x	x
1.0	x	x	x	x
1.3				x
1.5	x	x	x	
2.0	x	x	x	

- j_g and j_f are defined at the 3 in. diameter riser.
- In most cases the downcomer water level was varied from 0 to 60 in.

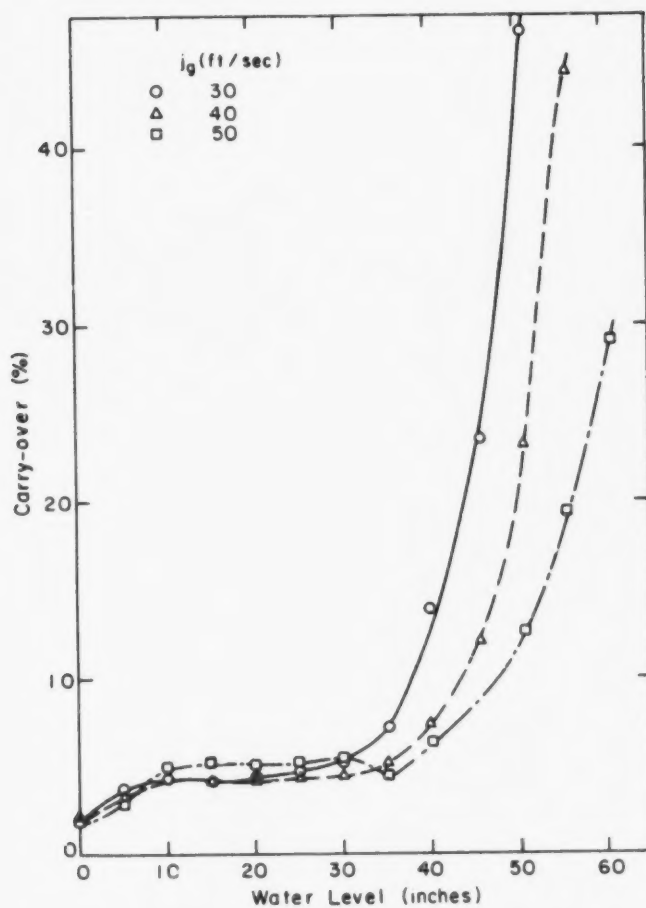


Figure 6 Carry-over Versus Water Level for $j_f = 1.0$ ft/sec in the Westinghouse Type Separator

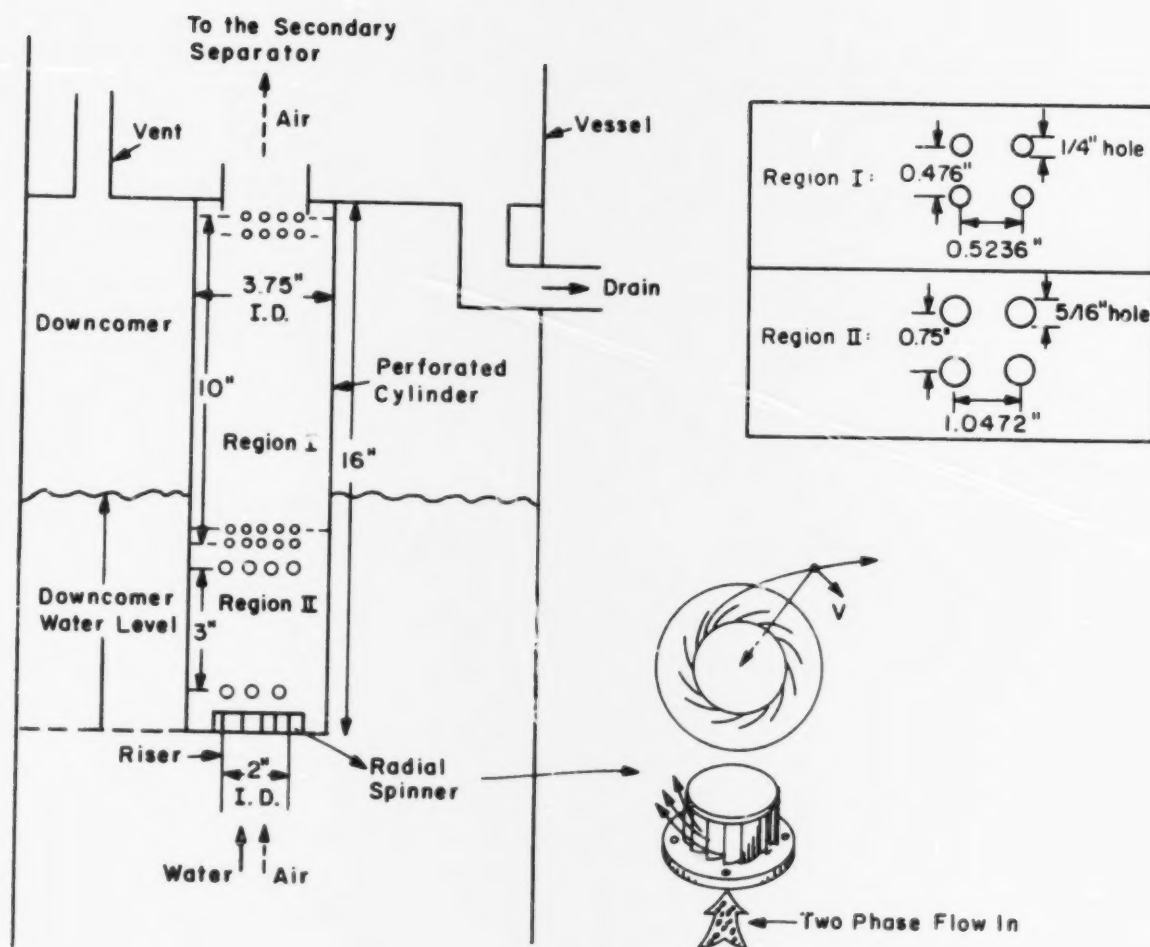


Figure 7 Combustion Engineering Type Separator

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downcomer water level for a range of j_g and j_f was shown in Figure 8 and 9. The downcomer water level was defined from the bottom of the separator cylinder. Similar to the cyclone and Westinghouse separator, the downcomer water level has the most profound effect on the separator performance. When the downcomer water level is below the middle of the cylinder, the efficiency of the separator is nearly 100%. As the downcomer water level goes up beyond that, it can be seen that a change of a few inches of the water level results in a dramatic degradation in the separator performance. In Figure 8, similar to the cyclone, the effect of j_g on the carry-over is minor to that of the downcomer water level. However, there is a great effect of j_f on the separator performance (see Figure 9). For low j_f with high downcomer level, the carry-over is greater than 100% (i.e. more carry-over than inlet water flow). In the experiment, plastic strings were attached near the holes from the bottom to the top of the separator for visual observation. For the case of $j_f = 1$ ft/sec in Figure 9, there are three distinct axial regions: the bottom region where the water comes out from the separator, the middle region where the water goes into the separator from the downcomer, and the upper region where the water comes out again from the separator. For the case of $j_f = 3$ or 4 ft/sec, the water comes out from the separator for all axial regions. Simple analysis of mass and momentum balance across a hole shows that whether the water comes out from or goes into the separator depends on not only the pressure difference between the inside wall and the downcomer but also the tangential velocity of the water at the inside wall. As the water flow rate increases, the velocity of the water and water film thickness increase such that the pressure on the wall increase. The high pressure with large velocity on the wall can overcome the hydrostatic head imposed by the downcomer water level such that the water can travel through the hole from the inside of the separator to the downcomer.

3. DISCUSSION OF QUASI-STEADY-STATE EXPERIMENT RESULTS

All quasi-steady-state tests were performed with air-water mixture at atmospheric pressure. The difference in geometry of centrifugal separators could provide different means of creating a swirling flow and also different ways of draining separated water. Despite these differences, the most important flow parameter affecting the performance of the separator is the downcomer water level in all three experiments. The effect of j_g and j_f are different for each separator. Especially for the CE type separator, the effect of j_f is much stronger than those of the cyclone and W type separator. However, the effects of j_g and j_f are minor compared to the one of downcomer water level.

Below certain values of downcomer water level, all three separators show little carry-over: in CE separator, almost no carry-over occurs, while in others less than 10% of carry-over occurs. However, when the downcomer water level is beyond those critical values, all the separators' performance start to degrade rapidly. How much carry-over occurs or how the separator could fail may be different for each case. It seems obvious for all cases that the complete separation of water from air can be achieved as long as there is a enough drain capacity. When the downcomer water level goes up, the available head for the drainage is reduced such that it could accommodate less water

Table 3. Test matrix for CE separator.

j_f (ft/sec) \ j_g (ft/sec)	21	31.5	42	62	82
0	x	x	x	x	x
1	x	x	x	x	x
2	x	x	x	x	x
3	x	x	x	x	
4	x	x	x		

- j_g and j_f are defined at the 2" vertical riser.
- Downcomer water level was varied from 2" to 15" for each test series.

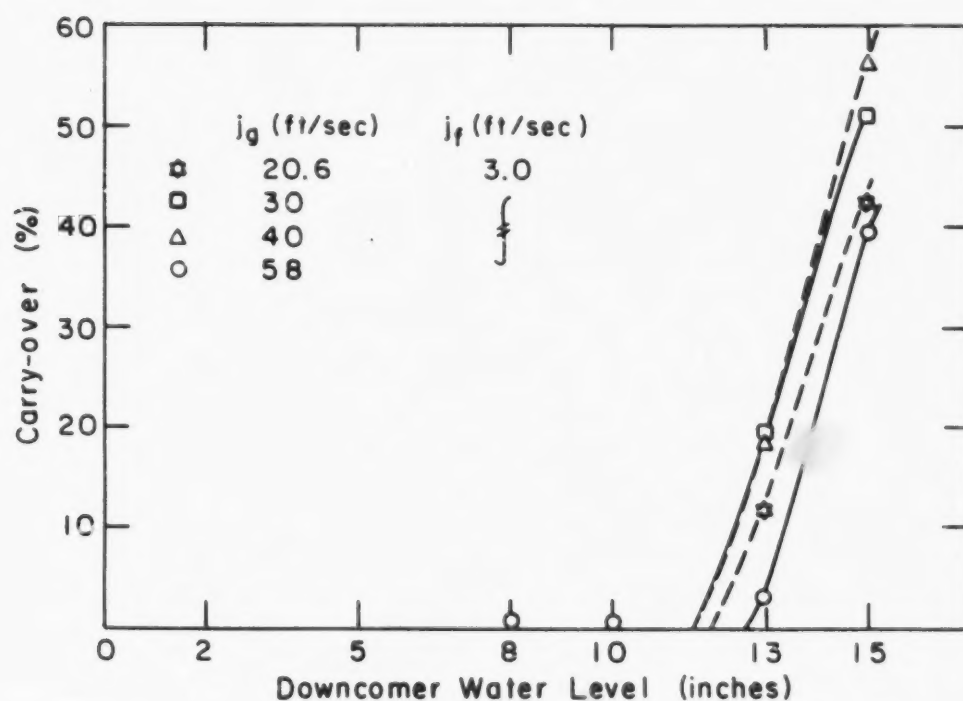


Figure 8 Carry-over Versus Downcomer Water Level for a Family of Air Flow Rates in CE Separator

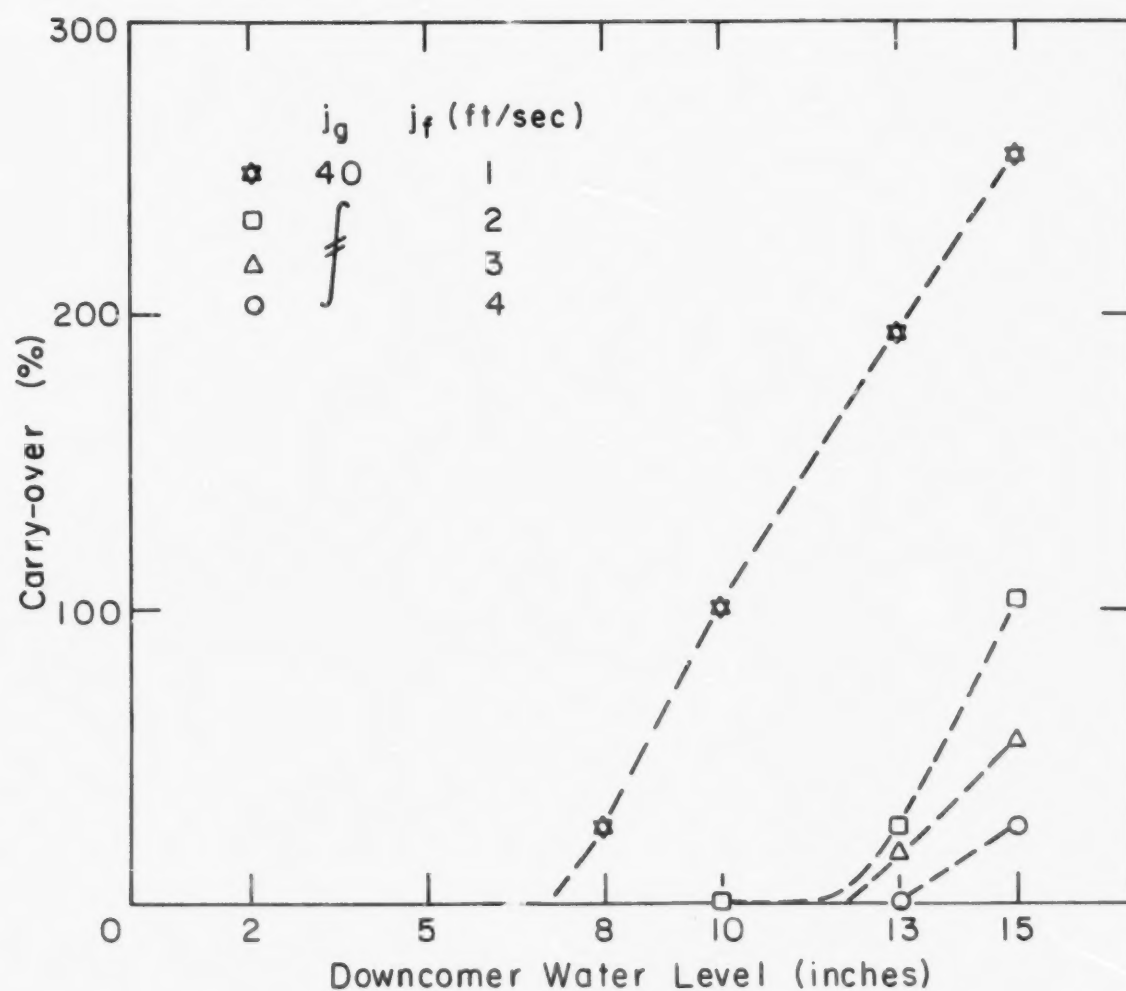


Figure 9 Carry-over Versus Downcomer Water Level for a Family of Water Flow Rates in CE Separator

flow. This is true for the cyclone and W type separator. However, for the CE separator, whether water can go out or not from the inside of the separator to the downcomer depends on the pressure difference across the perforated wall and tangential velocity of water at the wall. Because every perforated hole interacts directly with the downcomer water in the CE separator, how the water behaves in each hole could be different axially. In some cases of the cyclone experiment, it was found that the effect of ΔP across the deck plate on carry-over has the same effect as the hydrostatic head created by the downcomer water level. With high ΔP and high downcomer water level, the cyclone efficiency could go negative such that the amount of carry-over is more than the inlet water flow. Also, in some cases of CE separator experiment, the efficiency becomes negative with high downcomer water level. In these cases, the separator acts like a pump rather than a separator. Thus, even in the quasi-steady-state, flow reversal in drain line can occur. In some cases in the CE type separator, the carry-over changes from 0% to 100% within a few inches change of downcomer water level. This region compared to the whole steam generator is very small such that the performance can be changed like a sudden jump rather than a gradual transition.

Within a design envelope, the role of gravity and secondary separators' effects are less significant than that of the centrifugal separator. However, when a centrifugal separator's performance starts to degrade, the role of gravity and secondary separators could become very important up to a certain point. When the water comes out from a outlet of the centrifugal separator, it has both axial and tangential velocity. The effect of the tangential velocity enhances the gravity separation taking place above the deck plate because a centrifugal force due to the tangential velocity creates a radially outward velocity. From the cyclone experiment, it was found that the performance of the gravity separator largely depends on the drain capacity at the deck plate. Accurate performance can be predicted by using a simple pressure balance model that includes gravity head and frictional pressure drop. As long as there is a enough drain capacity for the gravity separation, the secondary separator could also be able to separate whatever water remained in the vapor. Further degradation of the centrifugal separator's performance by the increase of the downcomer water level cause so much water to carry over to the deck plate that it starts to accumulate on the deck plate and forms a pool. Until the pool reaches the top of the outlet of the centrifugal separator, the amount of carry-over from the system is still negligible. When the water level is further increased, the pool on the deck plate completely covers the top of the centrifugal separator outlet and two-phase mixture level forms. When the two-phase mixture level on the deck plate reaches the secondary separator, the continuous liquid with entrained droplets can go into the secondary separator. For this condition, the performance of the secondary separator depends on the drain capacity and entrained droplets are not important. Ultimately, water flow in excess of the capacity of the drain lines will be carried-over.

4. BLOWDOWN EXPERIMENTS

4.1 Apparatus

The experiments were conducted in the MIT blowdown rig. This rig basically is an upright 9.0 feet high stainless steel pipe of 3.8 inch inside diameter with an external downcomer pipe. The blowdown system was heated and pressurized by three 1.5 kW heaters. A detailed description of the apparatus is given in the reference in the Bibliography. A cyclone separator with drain and vent pipes for the deck-plate could be mounted in the rig. A bottom plate separated the upper downcomer region with the drains of the cyclone and the deck plate from the boiling region of the vessel. Figure 10 shows the experiment set-up and a sketch of the separator.

For the experiments the vessel was filled with water, sealed and heated. When the water reached desired saturation condition, a blowdown was initiated by opening a ball valve. The two-phase mixture could escape through a pipe in the top of the vessel which held an interchangeable nozzle to simulate various sizes of breaks. Pressure, temperature, collapsed liquid level beneath the separator, and the pressure drop across the separator were measured. A charging pump allowed the control of the initial water level before the blowdown. After the blowdown, the rig was drained to measure the remaining water directly.

Four different nozzle sizes and four different initial water levels were tested. The diameter ratio (d/D) of the nozzles d to the vessel D ranged from 0.024 to 0.137. The initial water levels were chosen with regard to the downcomer water level, which is a main factor affecting the centrifugal separator performance. The initial water levels of the experiment are marked in Figure 12. The separator impact was evaluated by running two similar blowdowns: one with and one without the separator for each combination of break size and water level. The saturation conditions before the blowdown were held constant at 800 psi and 518° F after we made sure that they do not influence the separator performance.

4.2 Results

The impact of the separator on a blowdown transient is small. The speed of the depressurization and accordingly the cool-down were unaffected. For all tested d/D ratios, the separator failed to hold back water in the vessel. On the contrary, it increased the total mass carry-over in most of the cases. However, the additional mass carry-over due to the separator never exceeded 10% of the initial inventory. To predict the total mass carry-over of the fast transient, the size of the discharging orifice and the ratio of initial liquid volume to total volume are more important factors than the presence of a separator.

In empty vessel blowdowns, the percentage of total mass carry-over is determined by the size of the discharging orifice and the initial water level. Carry-over increases with higher initial water levels and larger orifices. Similar behavior is true for blowdowns with a separator: the d/D ratio and

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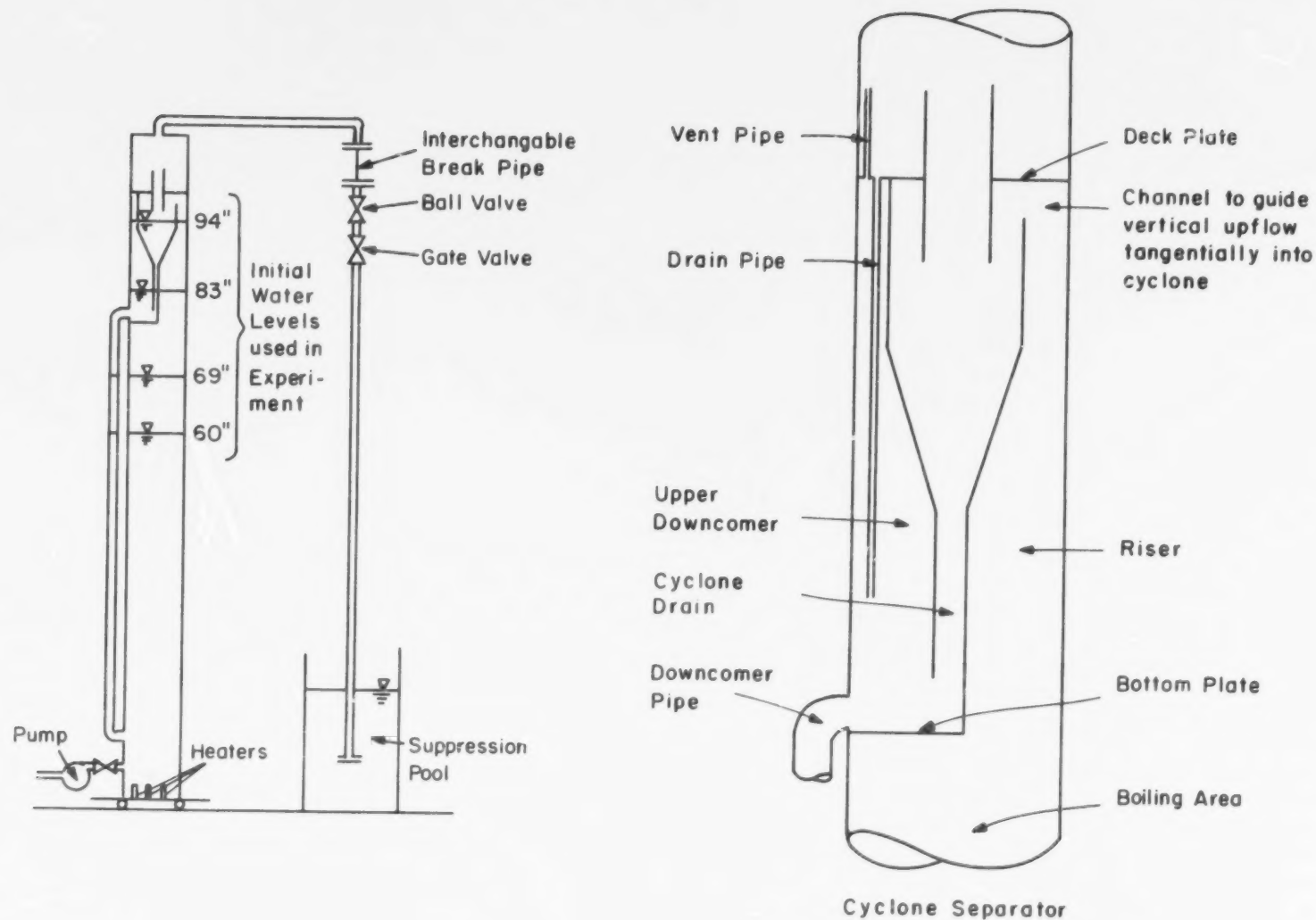


Figure 10 MIT Blowdown Rig with Separator

the initial water level govern the separator impact. The additional separator related carry-over is caused by liquid hold-up on the deck plate. Consequently a significant swelling of the two-phase mixture level over the separator deck plate is a major factor for an increase of the total mass carry-over.

The importance of the liquid hold-up increases for lower initial water levels and smaller orifices while the total carry-over decreases in these cases. However, if the orifice is too small to cause a high enough swelling of the mixture level, there is no liquid hold-up and accordingly no additional mass carry-over. On the other hand, for a medium break size the difference in total carry-over caused by the separator shrinks for higher water level (and a higher total carry-over percentage, see Figure 11.)

For a medium water level, however, it decreases for bigger d/D ratios and vanishes for very small d/D ratios which are beyond the boundary between blowdown transients and slow transients (see Figure 12). The break size at which the impact of the d/D ratio peaks depends on the initial water level. The smallest d/D ratio of the experiment of 0.024 was close to the transition from blowdowns to slow transients.

4.3 Conclusions

Blowdown transients are characterized by flashing and flow reversal in the downcomer and flooding of the separator. A computer model aiming at a prediction of the separator performance must focus on a flooding model for the deck plate and the gravity separator to quantify the liquid hold-up during the blowdown. Only a detailed model of the downcomer dynamics looking at vapor generation, mixture swelling, flow of downcomer mixture through deck plate vents, and the centrifugal separator model could provide the crucial information about the separator impact. In blowdown transients, a steam separator cannot be expected to reduce the total mass carry-over. It even leads to more carry-over compared to similar blowdowns without separator. However, the differences are not large. The separator impact is smaller than that of the initial downcomer water level and the size of the break area. Particularly the break size and subsequently the speed of the blowdown govern the total mass carry-over.

5. GENERAL STRUCTURE OF THE SEPARATOR MODEL

5.1 Separator Failure Scenario in a Fast Transient

Based on results from the quasi-steady-state and the blowdown experiments, the following separator failure scenario can be developed for the fast transients such as a steam line break or a steam line break combined tube rupture. It was assumed that a break is large enough to cause the fast transient including all effects typical for blowdown transients. For the quasi-steady-state transients, the failure scenario was described in Chapter 3.

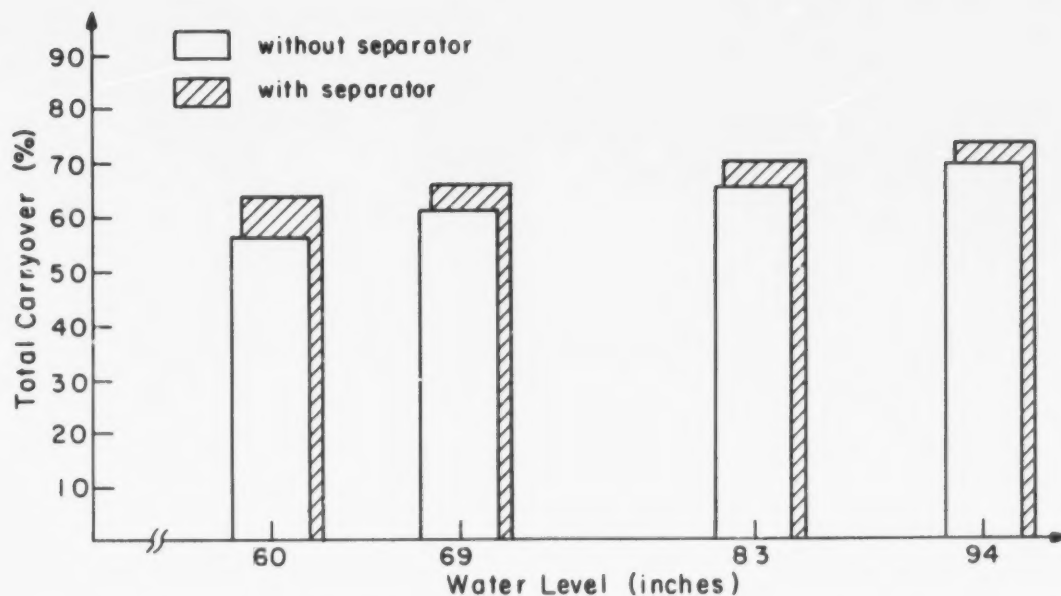


Figure 11 Total Carry-over Percentages depending on Initial Water Level for 1/4" Break with and without Separator (800 psia)

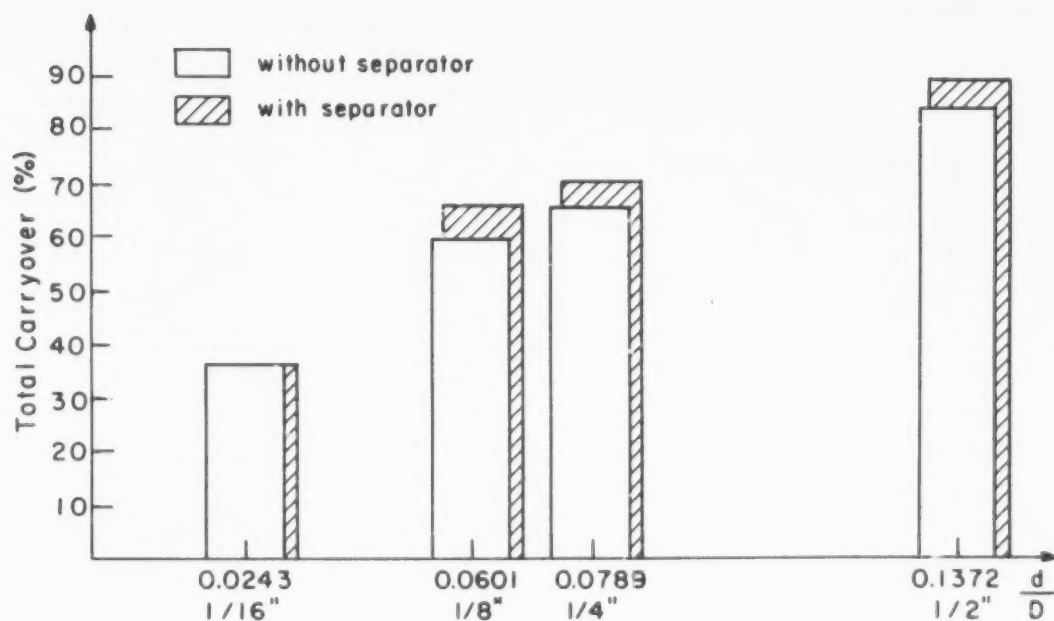


Figure 12 Total Carry-over Percentages depending on Break Size for 83" Initial Water Level with and without Separator (800 psia)

When the large break occurs, the system pressure drops, flashing occurs from all solid surfaces as well as within the body of water, and the flow rate through the separator can be up to several times of design value. The falling system pressure leads to vapor generation in the downcomer which creates a pressure drop across the deck plate and contributes to the rising two-phase mixture level. Soon, the two-phase mixture level in the downcomer rises beyond the deck plate and forms a pool at the deck plate. There is another factor that contributes to forming the pool at the deck plate. The high downcomer two-phase mixture level with ΔP across the deck plate reduces the hydrostatic head for the drain line from the centrifugal separator. This reduces the flow rate out the drain, causing more water to collect on the deck plate. As the carry-over from the centrifugal separator increase, the flow rate through the deck drain line increases such that it requires more hydrostatic head to drain it. As a result, water starts to accumulate on the deck plate and forms a pool at some point. Until the pool reaches the top of the outlet of centrifugal separator, the amount of carry-over from the system is still negligible. This is true because both the gravity separator and the secondary separator are still effective in removing any liquid entrainment.

Further increases in pool level whether it is due to rising downcomer water level or degradation of centrifugal and gravity separator's performance diminish the effectiveness of centrifugal separator and a possible flow reversal in the separator drain line could occur. Then, the centrifugal separator ceases to function as the separator and acts like a pump. At this point, the gravity separator still works with reduced capacity because of density difference between the drain line and the downcomer. As soon as the pool at the deck plate reaches the inlet of the secondary separator, the continuous water and entrained droplets carried into the secondary separator such that the hydrostatic head required to drain incoming water flow is larger than that available. As a result, the bottom of the secondary separator floods and the carry-over from the system increase rapidly. When this occurs the separator system can neither prevent the two-phase mixture in the steam generator from rising to the top, nor slow its process out of the steam generator.

During the blowdown, the void fraction in the steam generator increases constantly, which eventually leads to the collapse of the mixture level. As soon as the downcomer mixture level starts falling, the separator will start to function again. However, high steam flow generated in the downcomer could keep the pool above the deck plate for a considerable time even after the downcomer water level had receded below the separator inlet. At this point, two mixture levels exist in the steam generator: the main downcomer water level and the pool level at the deck plate. Further drops in the downcomer two-phase mixture level uncover the drain exits so that the separated liquid will have difficulty flowing down against the wind of steam coming up the drain lines. Possible flooding can then occur in the drain lines. The pool above the deck plate eventually disappears and the high quality flow through the separator make the overall effect of the separator system small.

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5.2 General Structure of the Separator Model

A simple and physically based separator model that is suitable for system codes such as TRAC or RELAP is being developed to predict the separator efficiency in an extreme range of conditions from slow quasi-steady-state transients to fast transients. The model consists of three stages of separation: centrifugal separation, gravity separation, and impingement separation. The model can be programmed on a subroutine in those system codes. The model stresses the importance of the total separation efficiency rather than that of each individual separation stage. It is also based on the principle that the performance of the separator depends on the capacity of the drain such that appreciable carry-over from the separator section of a steam generator occurs when the drain lines from all three stages of separation are unable to carry off the liquid flow. It was found that the overall pressure distribution determines the liquid removal capability of the centrifugal steam-water separator. Thus, it is necessary to have a pressure drop model to calculate the drain capacity specially in the cyclone and Westinghouse type separator. Therefore, the centrifugal model consists of the pressure drop model and the efficiency model.

In an overall model, it was assumed that there is no carry-under in the drain lines. It was found the carry-under decreases when the downcomer water level goes up. Also, when there is carry-under, the total carry-over from the system is negligible in MIT air-water experiments and other experiments. A module showing proposed model structure is given in Figure 13. Each separator model includes geometric variables and different models can be used for the different types of a centrifugal separator.

The model structure is one-dimensional such that a gravity separator can be modeled as one control volume and the other two separators can be modeled as control volumes or at the boundary junctions. With given geometric variables, the following boundary conditions are required: j_g and j_f at the riser, system pressure, downcomer water level and void fraction, and the amount of vapor generation in the downcomer specially in the fast transient. For the given boundary conditions, the maximum flow rate in each of the drain lines can be calculated. If the incoming flow rate is larger than the maximum flow rate that the drain can take, the excess flow will be carried-over. Depending on the pressure balance in each drain line, the flow can either go down or up. Also, if there is flashing in the downcomer, flooding might occur depending on the conditions. In calculation of the efficiency of separator system, the pool level at the deck plate is the only time-dependent calculated variable. The model doesn't include the detailed calculations such as a droplet trajectory in the secondary separator because they don't change the answer much. However, the user can have this option added to detailed calculations without difficulty.

The models developed vary in degree of complexity. For the quasi-steady-state, the performances of individual separators can be very important. Thus, it is recommended to use a model which includes all three stages of separation and can calculate each separator's performance for given boundary conditions. The other is a single switch model such that the

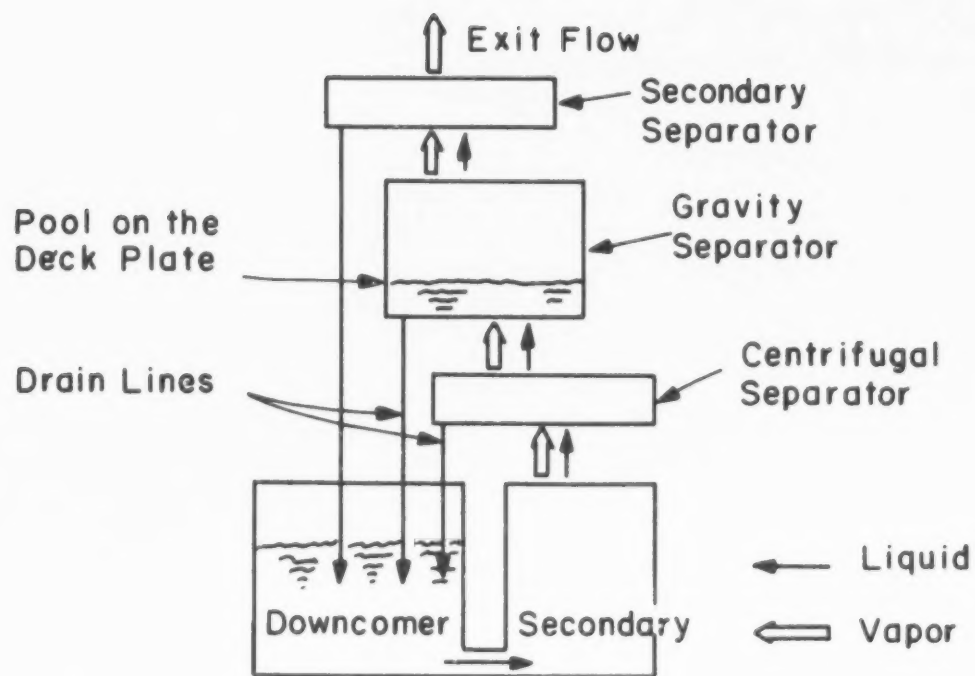


Figure 13 General Steam Separator Model Structure

separator is on or off depending on a critical downcomer water level which is a function of inlet conditions. Each separator will have a different critical water level. In the fast transient, the separator's performance changes rapidly in a very short region compared to the whole steam generator so that it could be adequate to use single switch model to predict the performance of the separator. Also, in the fast transient, the effect of the separator system diminishes when the whole separator system submerges under the water. The first model which is more refined than the single switch model also can be used in the fast transient. However, it is recommended to have a sensitivity study using system codes to compare the results from the first model with normal calculation or from the combination of both to the single switch model.

6. CONCLUSIONS

- (1) When the downcomer water level is low, the combined efficiency of the separators in series is practically 100%.
- (2) In every stage of separation, the downcomer water level is the most important parameter affecting the carry-over.
- (3) Water flows in excess of the capacity of the drain lines will be carried-over.
- (4) The pressure drop across the separator is important in the cyclone and Westinghouse separator but not in the CE separator.
- (5) The separator causes more total mass carry-over in fast transients than that of without separator. However, the effect is not large.
- (6) The downcomer flashing and flooding models are needed for the liquid hold-up in the deck plate during the fast transients.
- (7) The accuracy of the separator model is not important because the region where the separator performance changes is very small compared to the overall height of a steam generator.

7. BIBLIOGRAPHY

Liao, L-H, Parlos, A., and Griffith, P., Massachusetts Institute of Technology, "Heat Transfer, Carryover and Fall Back in PWR Steam Generators During Transients", USNRC report NUREG/CR-4376, EPRI NP-4298, September 1985.

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A Modified Drift-Flux Model and
a Weir Model to
Describe Two-Phase Flow in a Pipe
With Inverted U-bend

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I. Introduction

To understand the thermal-hydraulic behavior of a B&W reactor, it is important to have knowledge about the inventory distribution in the hot leg. For inventory calculation, and for phase separation, one needs to know the phase distribution in hot leg as the flow approaching the U-bend. The present study centers on the description of void and velocity profile in the hot-leg near the U-bend. The profiles were measured experimentally and an expression for the profile distributions was also derived analytically to represent the experimental data. The data were taken from air-water two-phase flow in a transparent U-bend channel of 3.5 in diameter. In this report, the following results will be presented

- (1) Analytical form of velocity profile in a curved channel,
- (2) Experimentally measured void and velocity profiles based upon simultaneous measurements through a combined pitot-tube conductance probe,
- (3) Comparison of cross-sectionally integrated void and velocity values with global measurements,
- (4) A modified drift-flux model for curved channel,
- (5) A weir-model to relate flow rate over the U-bend and the liquid level height at the U-bend.

II. Review and Analysis

A. Review

The drift-flux model was first proposed by Zuber and Coworkers (Refs. 1, 2, 3, 4) to relate void fraction and volumetric flow fraction (fraction of j).

$$\frac{J_g}{a_i} = C_0 J_M + \frac{\langle a_i V_{iD} \rangle}{\langle a_i \rangle} \quad (1)$$

$$\text{with } C_0 = \frac{\langle U_M a_i \rangle}{\langle U_M \rangle \langle a_i \rangle} \quad (2)$$

This equation can be used to determine the void fraction (or inventory) in the hot leg if the vapor velocity J_g is known. Since C_0 can be determined from integration of velocity profile and void profile, the C_0 varies accord-

ing to the flatness or pointedness of the profiles, as shown in Figure 1. The velocity and void profiles have been measured by Serizawa, (Ref. 5), Sim and Lahey (Ref. 6) Aoki et. al. (Ref. 7). But there was no measurement of these profiles in a curved channel, which is expected to deform the typical profile due to the secondary flow. The effect of curvature on C_0 was reported by Lin and Hsu (Ref. 8) thorough global measurements, as shown in Figure 2. In addition, in none of the above reference, simultaneous measurements were made on both local void fraction profile and velocity profile.

The void profile has been derived by Beattie (Ref. 9) to relate local void fraction with local liquid velocity. Ishii (Ref. 10) formulated a 3-D model. Drew (Ref. 11) extended Ishii work to obtain the lateral phase distribution in a circular tube. For curved channel, Gardner and Neller (Ref. 12) reported the competition between centrifugal force and gravitational force. Hoang and Davis (Ref. 13) measured the phase distribution in the neighborhood of the inverted U-bend.

B. Analysis for Profiles

The velocity field in a curved channel is a 3-dimensional problem. However, a simplified model can be used to produce the characteristic shape of velocity profile in a curved channel. Figure 3 shows the force vectors in a curved channel. The Navier-Stokes equation for the θ -dimension is

$$\begin{aligned} \rho \left[\frac{\partial v}{\partial t} + v_r \frac{\partial v_\theta}{\partial r} + \frac{v_\theta \partial v_\theta}{r \partial \theta} + \frac{v_r v_\theta}{\partial \theta} + \frac{v_r v_\theta}{r} + v_z \frac{\partial v_\theta}{\partial z} \right] \\ = - \frac{1}{r} \frac{\partial p}{\partial \theta} + \mu \left[\frac{\partial}{\partial r} \left(\frac{1}{r} \frac{\partial}{\partial r} (r v_\theta) \right) + \frac{1}{r^2} \frac{\partial v}{\partial \theta^2} + \frac{2}{r^2} \frac{v_r}{\partial \theta} + \frac{\partial^2 v}{\partial z^2} \right] + \rho g_\theta \end{aligned} \quad (3)$$

The equation can be considerably simplified if following assumptions are made:

- (1) flow is one-dimensional, in θ -direction, only
- (2) ρ and μ are constants
- (3) v_θ is only a function of r for a given
- (4) steady state

Then assume constant $dP/d\theta$ over a small sweep of angle

$$\begin{aligned} \frac{\Delta P}{\Delta \theta} &= \frac{(P - g r \sin \theta)}{\partial \theta} \\ &= P_{2,\theta} - P_{1,\theta} \\ &= - (P_{1,\theta} - P_{2,\theta}) = - \Delta P_\theta \end{aligned} \quad (4)$$

Integrate and use the assumption in $r \approx r - 1$, we obtain

$$v_{\theta} = - \frac{1}{\mu} (\Delta P_{\theta}) \left(\frac{r^2}{3} - \frac{r}{2} \right) + \frac{C_1 r}{2} \quad (5)$$

Use the boundary condition $v_{\theta} = 0$ at R ,

$$C_1 = \frac{\Delta P_{\theta}}{\mu} \left(\frac{2R}{3} - 1 \right) \quad (6)$$

$$\begin{aligned} v_{\theta} &= \frac{\Delta P_{\theta}}{3\mu} R^2 \left(\frac{r}{R} \right) \left(1 - \frac{r}{R} \right) \\ &= \frac{\Delta P_{\theta}}{3\mu} R^2 r^* (1 - r^*) \end{aligned} \quad (7)$$

$$= v_{\max, \theta} r^* (1 - r^*)$$

where $r^* = r/R$

The above equation is highly idealized, but it does yield a velocity profile of double pump. To make it more general, the velocity profile can be expressed as

$$v_{\theta} = v_{\max, \theta} r^* (1 - r^{*1/m}) \quad (8)$$

with m be determined empirically.

Similarly, since void profiles and velocity profile are similar in many cases, the void profile can be expressed as

$$\alpha = \alpha_{\max} r^* (1 - r^{*1/n}) \quad (9)$$

C. Weir Model

The liquid flowing over the top of the U-bend can be considered as one kind of weir. The local velocity of a weir is proportional to the elevation h over the weir, as shown by the Bernoulli equation

$$\frac{v_2^2}{2g} = \frac{(P_1 - P_2)}{\rho_L g} = \frac{\Delta P}{\rho_L g} = \frac{\rho_L g h}{\rho_L g} = h \quad (10)$$

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The model is shown in Figure 4. The flow over the particular weir of circular cross-section is the integration of flow as function of elevation

$$\begin{aligned}
 Q_L &= \int_0^H (C_D \sqrt{2gh})(2\sqrt{2R(H-h) - (H-h)^2}) dh \\
 &= \frac{32}{5} R^2 \sqrt{Rg} C_D \left[\frac{H}{3R} \left(1 - \frac{H}{2R}\right)^{1/2} - \frac{10}{3} \left(1 - \frac{H}{2R}\right)^{1/2} - \right. \\
 &\quad \left. \left(\frac{H}{2R}\right) \left(1 - \frac{H}{2R}\right)^{3/2} + \frac{10}{3} \right] \quad (11)
 \end{aligned}$$

Convert flow rate into superficial velocity, we have

$$\begin{aligned}
 J_L = \frac{Q_L}{\pi R^2} &= \frac{32}{5\pi} Rg C_D \left[\frac{2}{3} \left(\frac{H}{2R}\right) \left(1 - \frac{H}{2R}\right)^{1/2} - \frac{10}{3} \left(1 - \frac{H}{2R}\right)^{1/2} \right. \\
 &\quad \left. - \left(\frac{H}{2R}\right) \left(1 - \frac{H}{2R}\right)^{3/2} + \frac{10}{3} \right] \quad (12)
 \end{aligned}$$

then

$$\begin{aligned}
 \frac{J_L}{gR} \frac{1}{\frac{32}{5\pi}} &= \frac{Fr^{1/2}}{\left(\frac{32}{5\pi}\right)} \\
 &= C_D \left[\frac{2}{3} \left(\frac{H}{2R}\right) \left(1 - \frac{H}{2R}\right)^{1/2} - \frac{10}{3} \left(1 - \frac{H}{2R}\right)^{1/2} \right. \\
 &\quad \left. - \left(\frac{H}{2R}\right) \left(1 - \frac{H}{2R}\right)^{3/2} + \frac{10}{3} \right]
 \end{aligned}$$

Or, in term of Froude No.

$$\frac{Fr^{1/2}}{2.037} = C_D \left[\frac{2}{3} (h^*)(1-h^*) - \frac{10}{3} (1-h^*)^{1/2} - (h^*)(1-h^*)^{2/3} + \frac{10}{3} \right] \quad (13)$$

The result can be shown in graphical form, in Figure 5. Note that the Froude number is function of height of liquid over weir, as well as the drag coefficient C_D .

The weir model can be used to determine the liquid flow rate if the liquid height over the U-bend is known. Thus the U-bend with an liquid head

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measurement can be used as a flow measurement instrument.

III. Facility Description and Test Conditions

The test apparatus is a plastic pipe with inverted U-bend at the top. Air-water are injected from the bottom through an injection system with a honey-comb distributor (Figure 6). Three dp-measurement stations are positioned near the entrance to the U-bend for measurement of cross-section averaged void fraction. Air and water are separately introduced and metered (Figure 7).

For local void and velocity measurements, a pitot-tube and conductance probe combination (Figure 8) was developed. This probe is basically a pitot tube but with an additional wire to serve as an electrode for conductance measurement. Such combination enables simultaneous measurements of void and velocity at same location, which has not been done before by other investigators. The probe was traversed across the pipe along the diameter to give chordal distribution profiles. The traversing was done along two phases. The x-plane is in the same plane of the pipe sweep, the y-plane is perpendicular to the sweep. The flow conditions are:

Air	J_G	0 - 10 cm/sec
Water	J_L	0 - 40 cm/sec
Pressure		1 atm

These conditions covers the operation conditions to be expected from the UMCP 2x4 Loop during a small break LOCA and are in the similar ranges of conditions covered by Ishii's tests on U-bend.

IV. Test Results and Discussion

Test results are presented in the following four sections:

- Local void and velocity profiles
- Comparison of global measurements vs integration for local profiles
- Evaluation of C_0 through integration of local profiles Equation (2) as compared with the values from global measurements
- Weir flow results.

A. Local void and velocity profiles

These profiles were determined at four axial locations ($L/D=-3$, $\theta=15$, $\theta=30$ and $\theta=45$) in x-plane and y-plane, as described in Figure 9. Typical profiles are shown in Figure 10 to Figure 11. The salient features of the profiles are:

- (1) There is a dip of profile in x-plane, but less so in y-plane
- (2) The dip in x-plane is more pronounced with lower J_L for the same J_G and more pointed in y-plane for high J_L 's
- (3) The profile become more skewed as flow progressing into curve section

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- (4) Sometimes the negative flow can be observed
- (5) Void profiles show dipness and skewness with higher J_L (lower α)

B. Comparison of Integrated Values of α and μ with the Global Values

If the local profiles were measured in more planes than just x- and y-planes, a three dimensional profiles could be constructed through interpolation. With only x-plane and y-plane profiles, one can only obtain the cordal average values. Table 1 shows the α_x and α_y and the average $(\alpha_x + \alpha_y)/2$, in comparison with α which is the global value from dp-measurements. As shown in the last column the difference between the global average and the chordal average are mostly within 15%.

Similarly, the comparison between integrated average and the global liquid velocity are shown in Table 2. The difference for most cases is within 15% with the exception of a few cases.

C. Evaluation C_0 from Integration of an Idealized Local Profile with the Global Value

Recall in Section II that local profiles can be expressed in idealized form of Equations (8) and (9). If proper values of m' and n' are selected, C_0 obtained from integration of local profiles can be compared against the globally-measured C_0 . Table 3 shows such comparison. It should be noted that in Zone B, which is straight section, drift flux models are sufficient to give proper C_0 . In Zone A, however, the C_0 is decreased where curvature effect is manifested, the modified drift model has to be used.

Empirically, the m , n for drift-flux model in straight section (Zone B) and the m' and n' for modified (Zone A) are mainly function of Reynold's number Figure 12. The values of m , n , m' , n' in Figure 12 can be used for C_0 calculation, provided the flow conditions be in the same range with those used in the present study.

D. Weir data

Three sets of data were used to check the weir model: data from the plastic pipe of this study, data from UMCP 2x4 Loop and the data from Ishii's study with 4"-pipe. The comparison of data with the model are shown in Figures 13, 14, and 15. For each of the data set, a drag coefficient C_D has to be determined empirically first from one point and then the rest data points would fall into line. The data from Ishii's study is limited as only a short movie is available for data reduction.

There is at present, no model available to determine C_D other than empirically. However, from each facility, one C_D suffices.

V. Conclusion

In this study of two-phase flow in a pipe with U-bend, the following information have been obtained.

1. A modified drift model have been developed with the local velocity and void profiles be expressed as Equation (8) and Equation (9)
2. A combinations of pitot-tube and conductance probe has been developed to provide simultaneous local measurements of velocity and void profiles.
3. The local profiles showed dipness in the center as represented by the idealized profiles. They also show skewness as the flow enters the curved section.
4. Integration of local profiles given $\bar{u}$ and $\bar{u}$ which comparing favorably with the global measurements.
5. Proper values of exponents m , n , m' , and n' can be selected for drift flux and modified drift flux profiles for straight section and curved zone respectively to give C_0 which are close to globally-measured values. From these comparison, a set of empirical m , n , m' , n' can be selected and are found to be mainly functions of Re .
6. A weir model has been developed to relate the flow rate over the U-bend with the liquid height. Comparison of data from three facilities confirm the validity of the model if an empirical value of drag coefficient C_D can be selected for a given facility.

VI. References

- [1] Zuber, N. and J.A. Findlay, J. of Heat Transfer, Trans. ASME. pp 453, 1965
- [2] Zuber, N., and Findlay, J.A. "Average Volumetric Concentration in Two-Phase Flow Systems". J. Heat Transfer, 87c, 453, 1965.
- [3] Zuber, N. and J.A. Findlay, "The Effects of Non Uniform Flow and Concentration Distributions and the Effect of the Local Relative Velocity on the Average Volumetric Concentration in Two-Phase Flow", GEAP-4542, General Electric co., 1964.
- [4] N. Zuber and H. Hench, "Steady State and Transient Void Fraction of Bubbling Systems and Their Operating Limits," G.E. Rept. No. 62 GL 100. 1962.
- [5] A. Serizawa, "Fluid Dynamic Characteristics of Two-Phase Flow," Ph.D. Thesis, Institute of Atomic Energy, Kyoto University 1974.
- [6] S. Sim and R.T. Lahey, Jr., "The Measurement of Phase Distribution Phenomena in a Triangular Conduct," NUREG/CR-3576, 1973.
- [7] Shigebumi Aoki and Akira Inoue, "Flow Behavior and Phase Distribution in Two-Phase Flow around Inverted U-bend", Journal of Nuclear Science and Technology, 20 (11). pp. 915-928, Nov. 1983.

- [8] W.K. Lin and Y.Y. Hsu, "Two-Phase Flow Behavior in the Transparent U-Bend Pipe.", Proceeding of Third International Topical Meeting on Reactor Thermo Hydraulics. Newport, Rhode Island, Oct. 1985.
- [9] D.R. H. Beattie, "Two-Phase Flow Structure and Mixture Length Theory", Nuclear Engineering and Design, Vol 21, 1972.
- [10] M. Ishii, "Thermo-Fluid Dynamic Theory of Two-Phase Flow." Eyrolles, 1975.
- [11] D. Drew, S. Sim and R.T. Lahey, Jr., "Radial Phase Distribution Mechanisms in Two-Phase Flow." Proceedings of the OECD/CSNI Specialist Meeting on Transient Two-Phase Flow, Paris, June. 1978, CSNI Report No. 31.
- [12] Gardener, G.C. Neller, P.H. Roc. Inst., "Phase Distributions in Flow of An Air-Water Mixture Round Bends and Past Obstructions a the wall of a 76mm Bore Tube", Proc. Inst. Mech. Engn., 1984, pp 36., 93. 1969-70.
- [13] Hoang, K., Davis, A.R., Int. J. Multiphase Flow, 8, 267, 1930.

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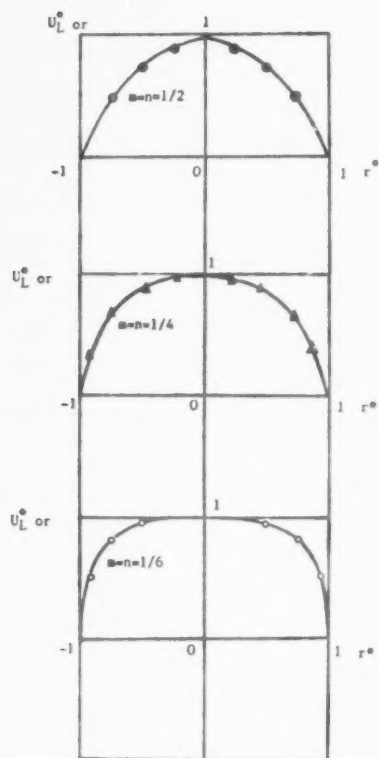


FIG. 4) THE VELOCITY AND VOID PROFILE WITH RESPECT TO DIFFERENT n, n VALUES FOR DRIFT FLUX MODEL

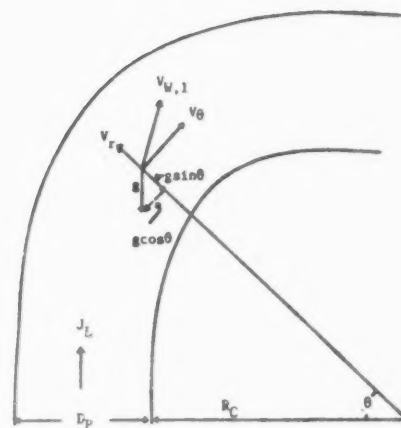


Fig. (3) FORCE GRADIENT ON THE FLOW DIRECTION IN THE U-BEND PIPE

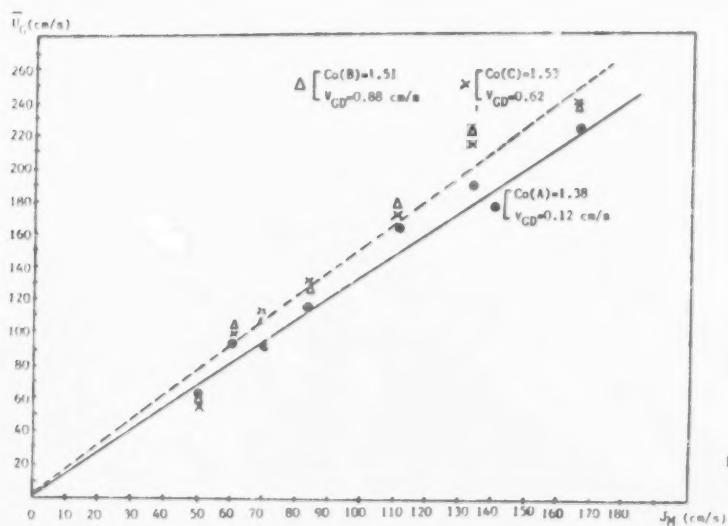


FIG. (2) CORRELATION OF HOLDUP IN UPWARD SLUG FLOW AT $J_L=37$ cm/s

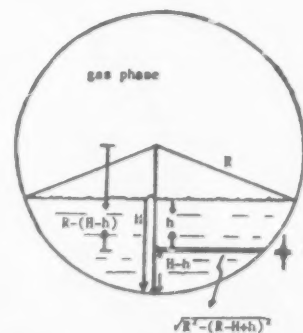


Fig. (4) CROSS-SECTION AREA OF THE TOP BEND

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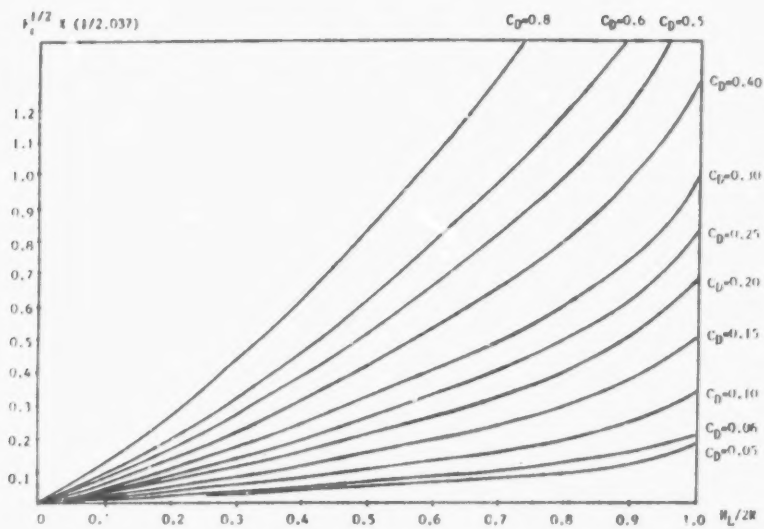


Fig. (5) THEORETICAL CURVE FOR WATER LEVEL V_b FUNCTION OF FROUDE NUMBER WITH DIFFERENT C_D VALUES AT $J_G = 0$ cm/s

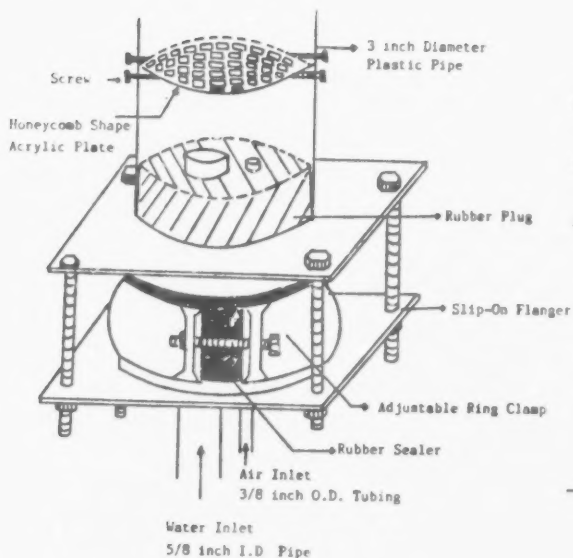


Fig. (6) BUBBLE INJECTION AND WATER INLET UNIT

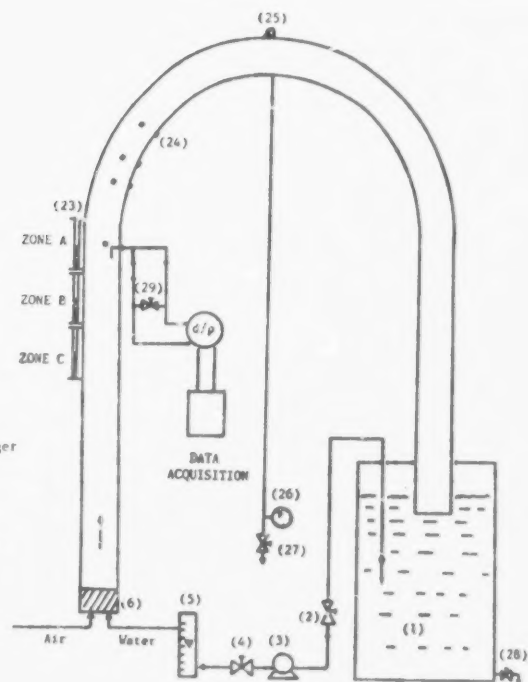


Fig. (7) SCHEMATIC DIAGRAM OF THE AIR-WATER LOOP

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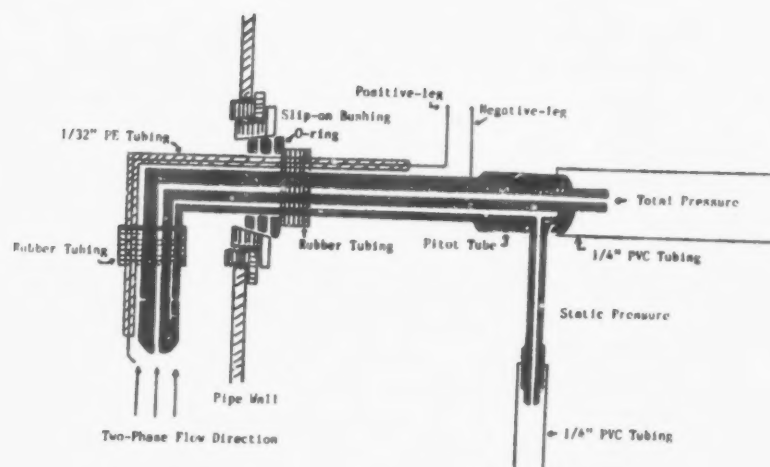


FIG. (8) THE PRESSURE AND VOID FRACTION DUAL FUNCTIONAL MEASURING PROBE

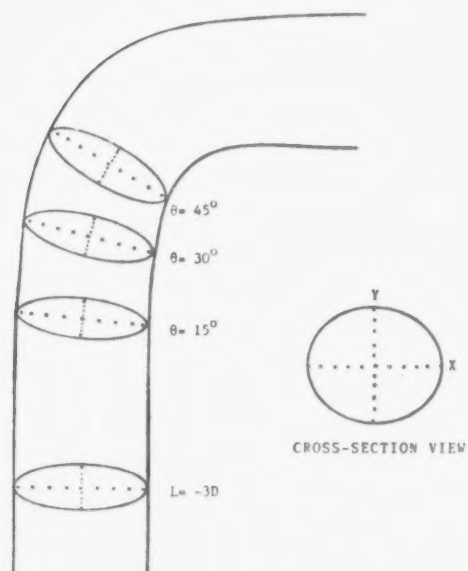


FIG. 9 THE MEASUREMENT POINTS FOR DIFFERENT ELEVATIONS AND X,Y ORIENTATION

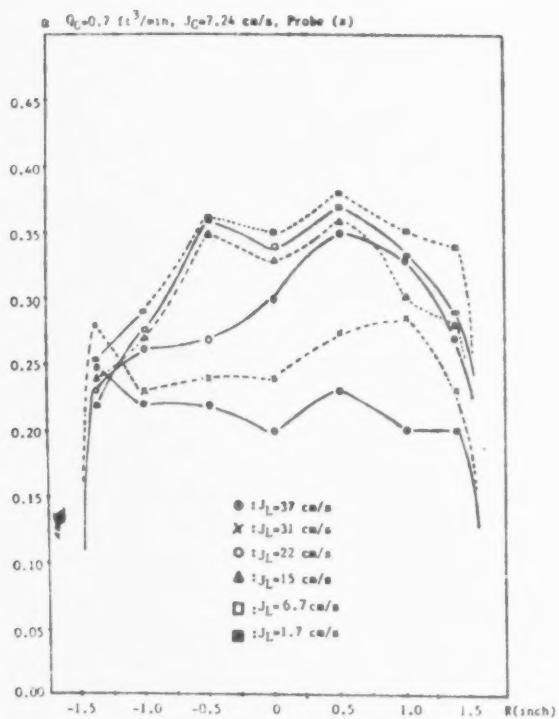


Fig. (10a) LOCAL VOID FRACTION PROFILE AT $L=3D$

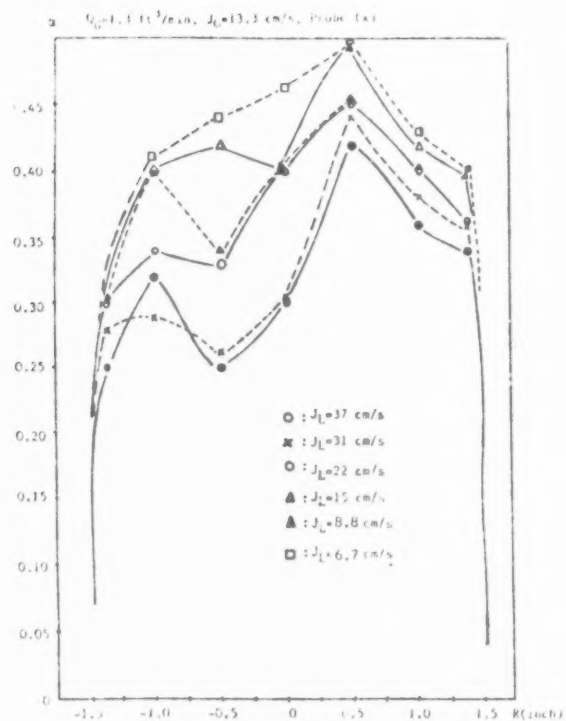


Fig. (10b) LOCAL VOID FRACTION PROFILE AT $L=3D$

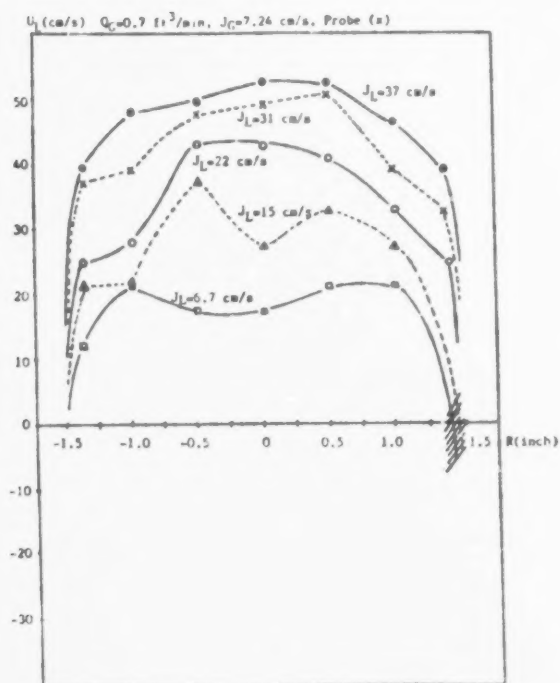


Fig. (11a) LOCAL VELOCITY PROFILE AT $L=3D$

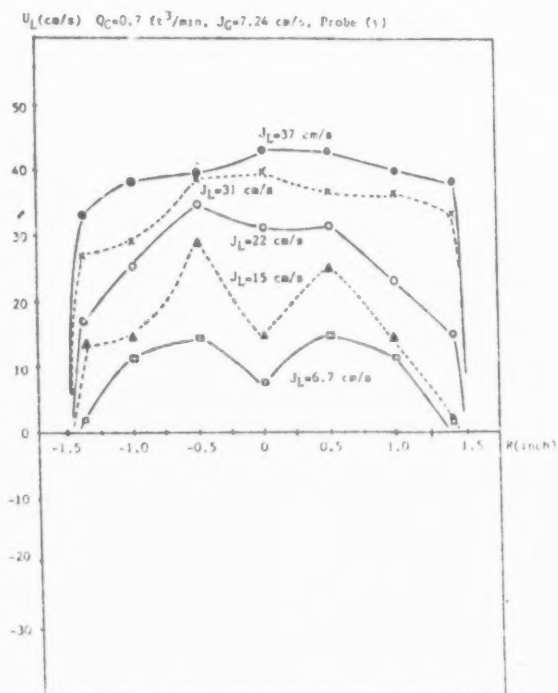


Fig. (11b) LOCAL VELOCITY PROFILE AT $L=3D$

Fig. 12 THE EXPERIMENTAL C_o VALUE WITH RESPECT TO REYNOLDS NUMBER BASED ON J_L

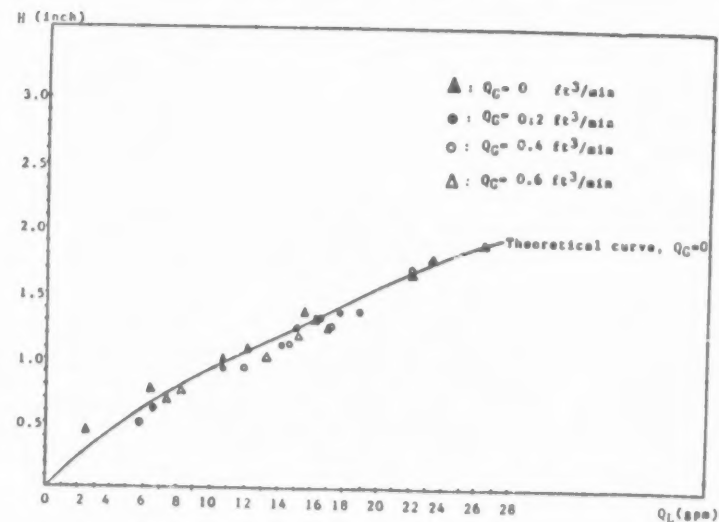
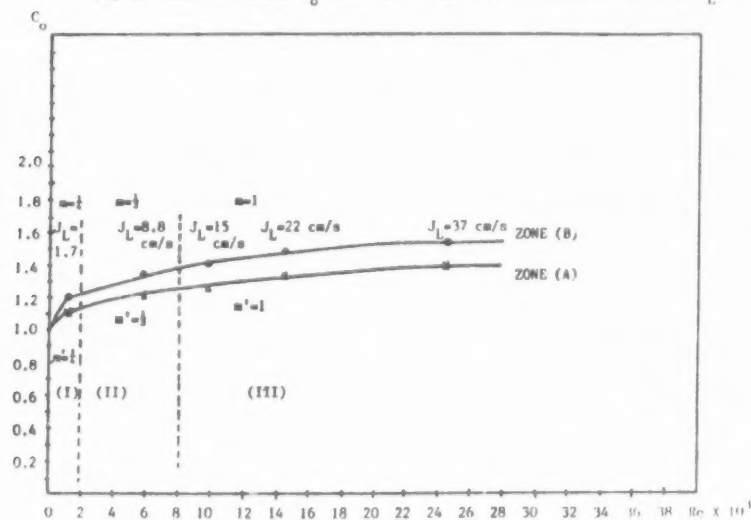


Fig. (13) 3" PLASTIC LOOP WITH $C_D = 0.203$ for H Vs. Q_L

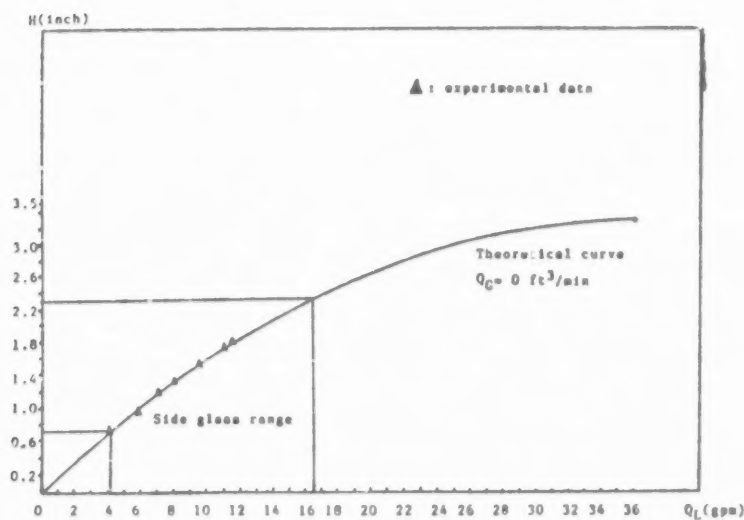


Fig. (14) UNIVERSITY OF MARYLAND 2X4 LOOP WITH $C_D = 0.09$ for H Vs. Q_L

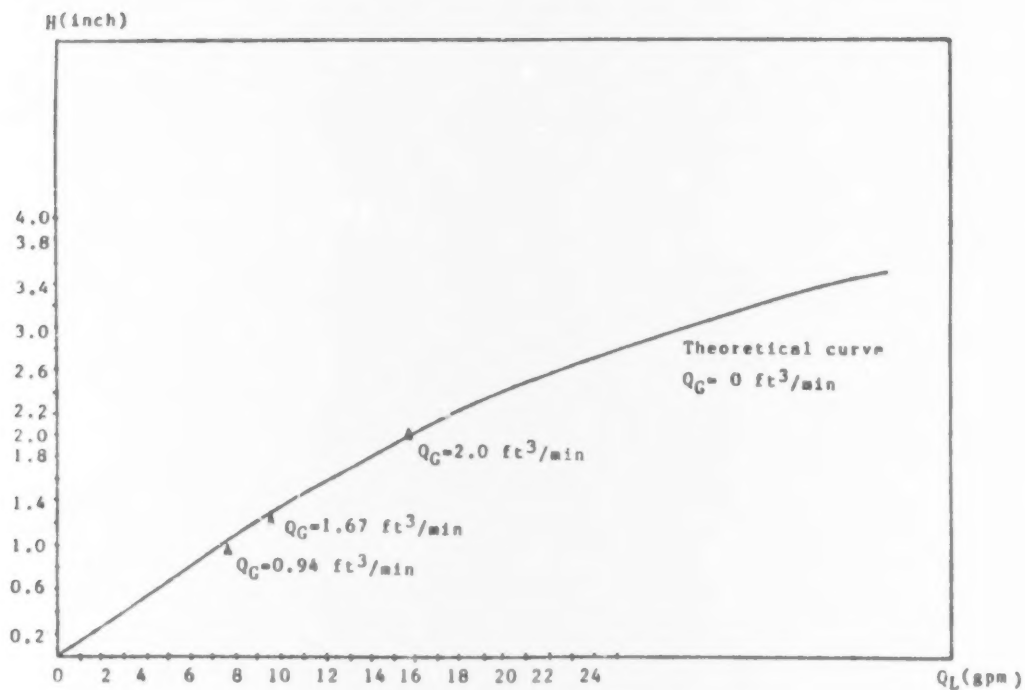


Fig.(15) ISHII'S 4th LOOP WITH $C_D = 0.06$ for H vs. Q_L

TABLE 1: COMPARISON OF THE INTEGRATION OF THE LOCAL VOID FRACTION ALONG THE X-AXIS, Y-AXIS WITH THE AVERAGE VOID FRACTION TAKEN FROM DP-UNIT (ZONE A) FOR THE TYPICAL CASE $J_G = 13.3 \text{ cm/s}$

$J_G \text{ (cm/s)}$	AVERAGE VOID-FRACTION, ZONE (DP-UNIT) α'	$\frac{\int \alpha(x) dx}{\int dx} = \bar{\alpha}_x$		$\frac{\int \alpha(y) dy}{\int dy} = \bar{\alpha}_y$		AVERAGE OF THE $\alpha(x)$ and $\alpha(y)$	
		$\bar{\alpha}_x$	$(\alpha' - \bar{\alpha}_x)/\alpha'$	$\bar{\alpha}_y$	$(\alpha' - \bar{\alpha}_y)/\alpha'$	$\bar{\alpha} = (\bar{\alpha}_x + \bar{\alpha}_y)/2$	$(\alpha' - \bar{\alpha})/\alpha'$
37	0.23	0.3	30 %	0.20	13 %	0.25	8 %
31	0.26	0.31	19 %	0.21	19 %	0.26	0 %
22	0.29	0.35	20 %	-	-	-	-
15	0.38	0.36	5 %	0.3	21 %	0.33	13 %
8.8	-	0.40	-	-	-	-	-
6.7	0.32	0.42	31 %	0.33	3 %	0.37	15 %
1.7	0.52	-	-	-	-	-	-

TABLE 2. COMPARISON OF THE INTEGRATION OF THE LOCAL SUPERFICIAL VELOCITY ALONG THE X-AXIS, Y-AXIS WITH THE EXPERIMENTAL DATA J_L VALUES, $Q_G = 0 \text{ ft}^3/\text{min}$

LOCATION	J_L' (exp.) (cm/s)	$J_{Lx} = \frac{\sum_{j=1}^N U_L(x_j)(1-\alpha(x_j))}{N}$		$J_{Ly} = \frac{\sum_{j=1}^N U_L(y_j)(1-\alpha(y_j))}{N}$		AVERAGE OF THE J_{Lx} AND J_{Ly}	
		J_{Lx}	error= $ J_{Lx}-J_L' /J_L'$	J_{Ly}	error= $ J_{Ly}-J_L' /J_L'$	$J_L = \frac{J_{Lx}+J_{Ly}}{2}$	error= $ J_L-J_L' /J_L'$
-3D	37	42.5	15%	38.9	5%	40.7	10%
-3D	31	34.3	10%	30.4	1%	32.35	5.5%
-3D	22	26	20%	21.9	0%	23.95	10%
-3D	15	16.6	11.1%	19.0	27%	17.8	19%
-3D	8.8	9.6	9%	9.8	12%	9.7	10%
-3D	6.7	5.7	13%	6.1	8%	5.97	10%

TABLE 3. COMPARISON OF THE THEORETICAL VALUE C_o FOR THE ZONE A AND ZONE B WITH THE EXPERIMENTAL C_o VALUES

$Re = \frac{J_L D_{12}}{\mu_L}$	J_L (cm/s)	THEORETICAL MODEL	THEORY	EXP. BY eq. 11-3.21	THEORETICAL PROFILE	ERROR= $ C_o - C_o' /C_o'$
5831	8.8	DRIFT FLUX $m = \frac{1}{2}$ $n = \frac{1}{2}$	$C_o(B) = 1.33$	$C_o'(B) = 1.33$		0%
		MODIFIED DRIFT FLUX $m' = \frac{1}{2}$ $n' = \frac{1}{2}$	$C_o(A) = 1.17$	$C_o'(A) = 1.16$		0.86%
		DRIFT & MODIFIED DRIFT $m' = \frac{1}{2}$ $n' = \frac{1}{2}$	$C_o(A) = 1.14$	$C_o'(A) = 1.16$		1.7%
1120	1.7	DRIFT FLUX $m = \frac{1}{2}$ $n = \frac{1}{2}$	$C_o(B) = 1.2$	$C_o'(B) = 1.2$		0%
		MODIFIED DRIFT FLUX $m' = \frac{1}{2}$ $n' = \frac{1}{2}$	$C_o(A) = 1.14$	$C_o'(A) = 1.11$		2.7%
		DRIFT & MODIFIED DRIFT $m' = \frac{1}{2}$ $n' = \frac{1}{2}$	$C_o(A) = 1.09$	$C_o'(A) = 1.11$		1.8%

SIMULATION EXPERIMENTS FOR HOT-LEG U-BEND TWO-PHASE FLOW PHENOMENA

by

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ABSTRACT

In order to study the two-phase natural circulation and flow termination during a small break loss of coolant accident in LWR, simulation experiments have been performed. Based on the two-phase flow scaling criteria developed under this program, an adiabatic hot leg U-bend simulation loop using nitrogen gas and water and a Freon 113 boiling and condensation loop were built. The nitrogen-water system has been used to isolate key hydrodynamic phenomena from heat transfer problems, whereas the Freon loop has been used to study the effect of phase changes and fluid properties. Various tests were carried out to establish the basic mechanism of the flow termination and reestablishment as well as to obtain essential information on scale effects of parameters such as the loop frictional resistance, thermal center, U-bend curvature and inlet geometry. In addition to the above experimental study, a preliminary modeling study has been carried out for two-phase flow in a large vertical pipe at relatively low gas fluxes typical of natural circulation conditions.

I. INTRODUCTION

In view of the impracticality of full-scale testing, scale models of a prototype system have been extensively used to predict the behavior of nuclear reactor systems during normal and abnormal operations as well as under accident conditions. The severity of the accident that occurred at the Three Mile Island Unit-2 plant has increased interest in this area. New scaling criteria for a two-phase system have been developed based on a rigorous perturbation method by Ishii et al. [1,2]. This approach has been used [3,4] to evaluate the design parameters of the new 2 x 4 simulation loop under the MIST program [5]. In view of certain scaling difficulties and scaling distortions in the integral facilities [5,6], a supporting experimental study to investigate the hot leg U-bend two-phase behavior and associated scaling problems has been initiated at Argonne National Laboratory.

The major issues addressed under this project are the interruption and reestablishment of the loop two-phase natural circulation. The void distribution contributes to the natural circulation force and is one of the major factors in determining the flow rate, interruption and resumption. Therefore, a precise understanding of the two-phase flow regimes, regime transition points and relative motion between phases are indispensable for accurate

predictions of the void fraction in the hot leg and overall loop flow behaviors. It is noted that in the prototype system, the diameter of the hot legs is about 90 cm and the length to diameter ratio relatively small at about 20. This indicates that the two-phase flow in the hot leg can be quite different from the standard small scale experiments. There is a great uncertainty in describing the flow because the conventional two-phase flow models and correlations are developed based on small diameter and large L/D system experiments. In view of this, the two-phase flow regimes in a hot leg is studied in detail in terms of the effect of the inlet geometry, axial flow regime development and length to diameter ratio.

II. BASIC SCALING CONSIDERATION AND SCOPE

The available approaches for developing scaling criteria for two-phase flow systems have been reviewed by Ishii and Jones [7]. The similarity analysis for system dynamical phenomena has been carried out by Ishii and Zuber [8] and for some local hydrodynamic phenomena by Zuber [9]. The results based on the one-dimensional drift flux model [10,11] and the perturbation method [8] were used by Ishii and Kataoka [1,2] to develop integral scaling criteria for overall loop thermal-hydraulic simulations.

In the present study, these scaling criteria for single and two-phase flows [1,2] are utilized. The two different flow visualization loops were designed according to the above scaling criteria. However, enough flexibility is built into the design such that various parametric effects and scale distortions can be studied by changing some components. The experiments are carried out in an adiabatic nitrogen gas-water loop and in a Freon-113 boiling and condensation loop.

For two-phase systems, phase change, subcooling, drift flux, Froude, friction, time ratio and heat source numbers are important [1,2]. The results of the scaling study for the B&W 2 x 4 loop design, see Fig. 1, show that the following scaling criteria should be satisfied [3,4].

The velocity ratio is given by

$$u_R = L_R^{1/2} \quad (1)$$

and the time scale ratio by

$$t_R = L_R^{1/2} \quad (2)$$

Here the subscript R denotes the ratio between the values for the scale model and the prototype, thus $\psi_R \equiv \psi_m / \psi_p$ where ψ is an arbitrary variable.

From the phase change number and subcooling number similarity, it can be shown that

$$\left(x \frac{\Delta \rho}{\rho_g}\right)_R = 1 \quad (3)$$

where x is the steam quality. This implies that the quality should be scaled by the density ratio $\rho_g/\Delta\rho$. In addition to the above similarity, if the drift-flux number similarity exists, then it can be shown that

$$\left(\alpha \frac{\Delta \rho}{\rho_f}\right)_R = 1 \quad (4)$$

Thus the void fraction is scaled by the density ratio $\rho_f/\Delta\rho$ which is almost always near unity. From this, the model void fraction should be very close to that of the prototype system.

In an adiabatic simulation, a gas phase is injected into a system instead of boiling. The injection rate should be related to satisfy the quality similarity given by Eq. (3). Then the injection rate in terms of the gas volumetric flux is given by

$$\left[\frac{(j_g)_{inj}}{u_f}\right]_m = \left(\frac{\Delta \rho}{\rho_g} x\right)_p \quad (5)$$

By knowing the steam quality in a prototype system $(j_g)_{inj}$ can be determined.

A detailed scale model study for the B&W plant design has been carried out using single phase and two-phase scaling criteria [3,4]. The most severe condition in terms of the thermo-hydraulic simulation is imposed by the friction number similarity, because in a scaled model the hydraulic diameter can be much smaller in a piping system. Therefore, for a given value of a_R , calculations are centered to determine x_R to meet $F_{iR} = 1$ for each section.

Then the maximum possible x_R for given a_R is given [3,4] by

$$x_R = 10.0 (1/a_R)^{-0.67} \quad (6)$$

and the volume ratio by

$$V_R = 10.0 (1/a_R)^{-1.67} \quad (7)$$

Hence, for a sample case of $V_R = 1/815$, one obtains for the practically optimum case as

$$a_R = 1/218 \quad (8)$$

$$k_R = 0.27$$

And in this case, the time scale is reduced by

$$t_R = \frac{t_m}{t_p} = \sqrt{k_R} = 0.52 \quad (9)$$

Therefore, the key timings of various transients will be reduced by a factor of 0.52 in the scale model. The prototype hot leg diameter is 91.5 cm, vertical rise 14.25 m and total length 21 m. By using these numbers, the corresponding model dimensions are 6.2 cm, 3.84 m and 5.6 m, respectively. In view of the commercially available glass tube sizes, d_m of 5.1 cm and 10.2 cm have been chosen. The vertical elevation of 3.5 m and 5.5 m are chosen to simulate the prototype flow as well as to study the scale distortion caused by using a too long hot leg such as in MIST [5].

Using these criteria, the data on void fractions, flow rates and pressure drops can be translated into prototype values. Furthermore, the information on hot-leg flow regime, U-bend flow separation, flow interruption and resumption can be recasted in terms of the prototype values. Same can be done for MIST and other simulation facilities. Furthermore, the experimental parametric study on the frictional resistance, thermal driving head, and various geometries such as the hot leg diameter, height and U-bend radius will clearly show the effects of scale distortions on these variables in definite numbers.

Another focus of the tests is the hot leg flow regimes and flow regime transitions. In the first series of tests, the gas is injected into the vertical section of the hot leg directly. The inlet section is always in the bubbly flow with a relatively uniform bubble size. From this controlled inlet condition, the entrance effect on the two-phase flow regimes and regime transitions can be easily studied visually. It is considered that the bubbly flow is the predominant flow regime in the hot leg of the prototype system. Hence these tests are very important for understanding the flow in the prototype system.

On the other hand, in the second series of tests, the vessel outlet and horizontal section of the hot leg are also simulated. This geometry may tend to produce a slug flow at the bottom of the vertical section of the hot leg. Although in the prototype system these slug bubbles should disintegrate immediately due to interfacial instabilities [12,13], this may not occur in the integral simulation facilities such as MIST, UM and SRI loops. Therefore, it is quite important to identify the effect of the vessel outlet and horizontal section on the two-phase flow regimes.

The third series of experiments are carried out in the Freon-113 boiling and condensation loop. The effects of phase changes and fluid properties can be studied in detail in comparison with the adiabatic loop experiments. Only preliminary tests are carried out in this boiling loop. However, several important differences have already been observed.

The data from these parametric study will be used to develop a two-phase flow model for hot leg and U-bend flow. A parallel analytical study is currently carried out to determine the pipe diameter and entrance length effects on the flow regime. From the combination of the data and analysis, the final two-phase flow model will be recommended. In this report, a limited number of data and flow observations are reported.

III. EXPERIMENTAL FACILITY

A. Adiabatic Simulation Loop

The test facility for studying hot leg U-bend two-phase flow was built in accordance with the scaling criteria developed by Ishii et al. [1-4]. As shown in Fig. 2, this two-phase flow loop consists of the bubble injection chamber simulating the core, 5 cm ID riser, inverted U-bend, gas separator simulating the once through steam generator, and cold leg return. The bubble chamber, riser and U-bend section were made of Corning Pyrex glass tubes and fittings, whereas the gas separator was made of plexiglass. The maximum riser (hot leg) height was about 5.5 m. The radius of the initial U-bend was 8.8 cm. However, the loop was designed such that the hot leg diameter, U-bend radius and hot leg height could be easily changed to study the parametric effects.

To model the steam/water flow in an actual LWR, in this experimental facility the tested fluids were nitrogen and water, working as the vapor phase and liquid phase, respectively. The gas was injected into the riser through nozzles which were made of stainless steel tubes, having a nominal 0.015 cm ID and 0.03 cm OD. These 625 nozzles were molded into an epoxy plate which was held by a plenum at the bottom of the riser. The gas flowrate and pressure were measured between the plenum and the gas accumulator. Downstream of the U-bend, there was a separator where the gas was vented to the atmosphere from the loop through a valve. This separator, made of transparent plastic, was 91 cm high and 20 cm in diameter. To direct the gas and liquid carry over from the U-bend into the separator, the vertical section was extended into the separator. This extension was made of a plastic tube of 5.1 cm diameter and 46 cm length. It was mounted to the separator cover and had many perforated holes of 1 cm in diameter such that the separation of gas from the gas-water mixture could be quickly achieved without disturbing the water level in the separator. Above the separator, a bellow type flexible coupling of 5 cm length was used to reduce the stress on the glass test section. Beneath the separator, there was a 60 cm long glass tube through which entrainment of gas phase into the cold leg was monitored. Beyond this point, a plastic tube of 5 cm in diameter was used instead of a glass tube since single phase flow could be expected. In the downcomer (cold leg) section there was a friction control valve which could be used to alter the loop frictional resistance. Furthermore, a liquid flow meter was installed in the horizontal section of the cold

leg with a sufficient straight entrance length. This flow meter is a paddle wheel type with magnetic pick-up and has special characteristics of very low pressure drops.

This loop can be operated either in a natural circulation mode or in a forced circulation mode. The natural circulation can be induced by a hydrostatic head difference between the riser section and the downcomer section. No external power is necessary under this mode. For a forced circulation mode, a centrifugal pump with the capacity of 76 litre/min was used. By properly adjusting the control valve and bypass valve, a forced circulation with a desired flowrate can be obtained.

In the riser section, seven pressure taps were installed for measurement of the differential pressure at five locations as indicated in the figure. These pressure taps were made of brass plate. The inside diameter is 5.1 cm and outside diameter is 8.9 cm. A small hole of approximately 0.4 mm in diameter was drilled to measure the pressure. At the low flowrates of interest to this experiment, these pressure taps give accurate void fraction measurements through differential pressure transducers because the frictional losses are negligibly small.

The experimental loop is designed to simulate the prototype system under natural circulation conditions. Thus the pump rotor is considered to be locked in the stationary position. The frictional valve fully opened position corresponds to the prototype system with the locked pump rotor.

In order to study the effect of vapor phase inlet section on the two-phase flow regimes and flow interruption three different inlet sections were used, see Figs. 3 and 4. The first type had no horizontal hot leg section to force the inlet flow to be in a bubbly flow. The latter two had a horizontal section of different lengths. A detailed description of the loop is given in Refs. [14] and [15].

B. Freon 113 Boiling and Condensation Loop

The basic dimensions of this loop are similar to the adiabatic loop described above. However, the boiling and condensation phenomena can be studied in addition to the capability of the nitrogen-water loop. As can be seen from Fig. 5, the loop has the boiler section with seven electrically heated rods of the total power of 4.2 KW and the condenser section cooled by the secondary Freon-113 loop. The heat is transferred to the secondary loop at the simulated steam generator from the primary loop. The heat sink of the secondary loop consists of the heat exchanger cooled by water and coils submerged in a cold bath within a large chest-type freezer. The coolant in the secondary loop is pressurized to stay in the liquid phase and circulated by a pump. The loops have various power and temperature measurements in addition to the hydrodynamic measurements such as the differential pressure transducers, pressure gauges and flow meters. The pressure lines have special bubble purging devices to eliminate gas or vapor from the transducers and pressure lines. All the two-phase flow sections are made of pyrex glass tubes and fittings such that the flow visualization experiments are possible.

IV. EXPERIMENT IN ADIABATIC LOOP

A. Range of Flow Parameters

Generally, this experiment was conducted on the basis of three varying parameters; they were gas flowrate, friction valve opening and water level in the separator. Also, in order to study the effect of vapor phase inlet section on the flow regimes and flow interruption, two horizontal sections of different lengths were tested, see Figs. 3 and 4. Table I lists the ranges of parameters in this experiment.

Table I. Range of Flow Parameters

gas volumetric flow	0 ~ 50 cm/sec
friction valve opening	1, 1/4, 3/16, 1/8
separator water level	18 cm, 45 cm, 80 cm
horizontal section length	0, 15 cm, 91 cm

Two U-bends with different curvatures, $R/d = 1.78$ and 3, were tested at several separator water levels against different friction valve openings and vapor flowrates for analyzing its effect on the flow termination as well as natural circulation flow.

A detailed description of experimental data are given in Refs. [14] and [15]. Only the summary of the results are given below.

B. Natural Circulation and Flow Interruption

In general, the induced liquid natural circulation rate decreases with the decreasing gas flow rate. As the gas flow rate decreases further, eventually the liquid circulation rate becomes zero corresponding to the flow interruption. When the permanent flow termination occurs, the two-phase level in the hot leg stays at or below the lower surface of the top of the U-bend. Due to the insufficient hydrostatic head difference between the hot leg and cold leg, the two-phase level cannot rise to flow over the U-bend section, thus there is no carryover liquid flow. Only the gas phase is transported over the U-bend. Two-phase flow oscillations are observed at near the flow termination condition. The amplitudes of the oscillations can be very significant. The period of the oscillations is about 20 sec.

Besides the permanent flow interruption discussed above, intermittent flow interruptions have also been observed in the slug or churn turbulent flow regime. As a large slug bubble passes over the U-bend, the section is temporarily voided and the liquid carryover flow becomes very small. However, with the passing of a liquid slug, the carryover flow is fully recovered. The

liquid natural circulation rate measured by the flow meter in the cold leg return shows that the flow is quite steady and not much influenced by the intermittent carryover at the U-bend. It appears that the liquid mass has sufficient inertia, thus the total liquid flow is not sensitive to the disturbances caused by each slug bubble. The time scale of the intermittency is about 1 sec whereas the flow oscillations have a period of approximately 20 sec. Thus it can be concluded that these two phenomena are not related.

The intermittent flow interruption does not lead to the permanent flow termination. This indicates that the phase separation in the U-bend is not the cause of the permanent flow termination. The present experiments have demonstrated that the natural circulation termination occurs when there is insufficient hydrostatic head in the downcomer side. As long as the two-phase level in the hot leg can be raised to the top of the U-bend, the carryover flow can be reestablished. This points to the importance of the thermal center in the once-through steam generator and the void fraction distribution in the hot leg.

C. Effect of Separator Water Level (Thermal Center)

The induced liquid flow rate increases with the increasing water level in the separator at a fixed gas flowrate and friction valve opening. The vertical distance between the U-bend and the bottom of the hot leg is fixed, thus an increase of water level in the separator results in an increase of the hydrostatic head in the downcomer. Since the natural circulation is induced by the static head difference in the hot leg and cold leg downcomer, the circulation rate increases with the level.

The induced liquid flow rate depends on the separator water level, frictional loss along the loop and void fraction distribution in the hot leg. The gas flow at the flow termination is reduced as the water level in the separator increases. Thus the flow termination condition conforms to the general parametric dependences of the natural circulation rate. This shows that at higher water levels, less gas rates are required to induce the same liquid flow rate.

D. Effect of Frictional Resistance

The changes in the frictional resistance of the test loop was achieved through adjusting the opening of the gate valve which was of screw type with a diameter of 5 cm. The openings of the gate valve was varied over fully, 1/2, 1/4, 3/16 and 1/8 open in terms of the valve stem rotations. For each position, tests were run with three different water levels in the separator and three different geometries of the core exit section. It has been observed that as the friction valve opening decreases (implying increases in the single phase friction resistance) the induced liquid flowrate decreases. However, such effect was not pronounced when the friction valve opening was at fully, 1/2 and 1/4 open, because over this range the frictional resistance does not change considerably. The extent of the effect of the friction valve opening on the induced liquid flowrate also depends on the separator water level. Furthermore, a decrease of the friction valve opening has a tendency to stabilize the flow oscillation. This effect applies to all three different

geometries of a core exit. Most importantly, the friction valve opening has no influence on the relation between liquid flowrate and gas flowrate when the flow termination is reached.

E. Effect of Inlet Section Geometry

Three different geometries of a core exit section were tested to compare their effect on the flow interruption and induced liquid flowrate. One had a vertical inlet where the gas flowed vertically into the hot-leg as shown in Figs. 2 and 3. Two of them had a simple mixing chamber, horizontal section and elbow, as shown in Fig. 4. The horizontal section had a length of 15 cm or 91 cm.. A 5 cm pipe diameter elbow of 90° and 18 cm radius was used between the vertical hot-leg and the horizontal section. The two-phase mixture of nitrogen and water flowed horizontally through this section before entering the vertical hot-leg.

With the vertical inlet section, the flow regime transition from bubbly to slug flow showed strong entrance effect. The axial transition position was a function of the gas flux. However, at the top of the hot leg the general trend of flow regimes can be predicted by existing criteria [16].

In the present experiments, the hot leg flow with a horizontal section is always in the cap bubbly or slug flow regime. This is due to the flow stratification at the horizontal section. The flow regime in the horizontal section is either elongated bubbly flow or stratified flow with a propagating interfacial wave front. The stratification occurred within about one diameter of a pipe from the simulated core exit plane. However, in general this length depends on the bubble rise velocity and liquid flux. At the elbow the horizontal two-phase flow is turned 90° into a vertical flow. At this point quasi-periodic slugging occurred. This slugging produced either cap bubbles or slug bubbles depending on the gas flux. Also it has been observed that smaller cap bubbles tend to catch-up the preceding ones and agglomerate into slug bubbles.

According to a recently developed theory by Kocamustafaogullari and Ishii [12,13], the maximum cap bubble size is about $40 \sqrt{\sigma/g\Delta\rho}$. Thus for a vertical pipe much larger than this value (~10 cm), the stable slug flow cannot be sustained. Due to the instability, the initial large bubbles should disintegrate into smaller cap bubbles of stable sizes. It is expected, therefore, in the prototype system with a diameter of 91 cm the slug flow is unstable in the vertical hot leg section. A typical flow regime should be a bubbly or dispersed cap bubble regime. On the other hand, in the integral test facilities, the slug flow is more stable. Thus both the diameter of the hot-leg and the inlet geometry have significant influence on the flow regimes, void distribution and flow behaviors.

F. Effect of U-bend and Its Curvature

In this experiment most of the experimental trials were performed with the same U-bend, however a modified U-bend of different curvature was also tested at separator water level of 45 cm and 18 cm under varied friction valve openings to compare the effect on the flow behavior and flow termination. The

ratio of the curvature radius to diameter was 1.78 and 3 where the one with 1.78 was considered as the standard.

Comparisons of the amplitudes of the induced flow oscillation and superficial liquid velocities between tests with these two U-bends show no significant differences when the separator water level was 45 cm. The average behaviors were quite similar in terms of the flow termination and natural circulation rate. Only some differences in dynamical behaviors have been observed. It shows that the U-bend with larger curvature tends to broaden the range of the superficial liquid velocity over which the flow oscillation can occur. The experimental data and the results of the flow visualization have confirmed that the flow termination is mainly governed by the loopwise hydrostatic head balance and not by the local phase separation phenomena at the U-bend. It appears, therefore, the detailed geometrical scaling of the U-bend section may not be very important for studying the overall loop behavior.

G. Simple Model for Flow Termination

A simple model for the prediction of the flow termination can be developed from a force balance. For a steady state natural circulation flow, the hydrostatic head difference between two legs should balance the frictional pressure drop along the loop. Thus

$$\rho_f l_w g - \int_0^{l_{\max}} \rho_m g dz = \left[\frac{f_{SP} l_{SP}}{2D} + \frac{K}{2} \right] \rho_f u_f^2 + \int_0^{l_{\max}} \frac{f_{TP}}{2D} \rho_m u_m^2 dz \quad (10)$$

Here ρ_w and $\rho_{\max}$ are the height of the separator water level and hot-leg U-bend from the bottom of the loop, respectively.

At the point of the flow termination $u_f = 0$, thus it reduces to

$$\rho_f l_w g - \rho_m l_{\max} g = \left[\frac{f_{TP} l_{\max}}{2D} + \frac{K_{TP}}{2} \right] \rho_m u_m^2 \quad (11)$$

Here it has been assumed that the mixture density ρ_m is approximately uniform along the hot leg for simplicity. Under the low gas flux condition typical of the flow termination, the two-phase frictional pressure drop may be neglected with respect to the gravitational terms.

Then from Eq. (11) it can be shown that

$$\alpha \approx \frac{\rho_f}{\Delta \rho} \left[1 - \frac{l_w}{l_{\max}} \right] \quad (12)$$

The comparison of the predicted void fraction at the flow termination to the experimental values under different conditions are given in Table XII. As can be seen from the table, the experimental mean void fractions in the hot-leg agrees with the predicted values from the simple model. In Table II, the experimentally measured void fractions at five different levels are given. The predicted void fraction is the overall average void fraction along the entire hot leg section.

Table II. Comparison of Void Fraction at Flow Termination Between Empirical Data and Prediction with Friction Valve Fully Open and without Horizontal Hot-Leg Section

Separator Water Level	j_g (cm/sec)	α_1	α_2	α_3	α_4	α_5	α_{pre}
80 cm	3.3	0.108	0.130	0.142	0.140	0.145	0.151
45 cm	5.4	0.244	0.309	0.195	0.198	0.200	0.215
18 cm	7.0	0.375	0.291	0.218	0.222	0.325	0.264

H. Flow Instabilities

At low gas flow rates under the natural circulation condition, large amplitude flow oscillations have been observed. The period of oscillation was between 10 and 25 sec. It was a weak function of the gas flux or liquid natural circulation rate. The period decreased as the gas flux increased. On the other hand, the amplitude strongly depends on the gas flux and inlet flow restriction. The general tendency was that the amplitude increased as the gas flux decreased until it hit the maximum value near the flow termination point. However, as the gas flux was further reduced toward the liquid flow termination, the amplitude slightly decreased. The inlet flow restriction stabilizes the flow both in terms of the amplitude and unstable range. With a larger flow restriction, this flow oscillation can be basically eliminated.

The period of the oscillations and parametric dependencies show that this instability is related to the void propagation phenomena [17,18]. The characteristic time can be related to the kinematic wave velocity and the system axial length. This implies that the period of oscillation is in the order of the mixture residence time. It is considered that the observed phenomenon is either the density wave oscillation or its coupling with the compressible volume in the separator.

In terms of the safety analysis for the prototype system, the occurrence of this flow instability is important. The dynamical behavior of the loop near flow termination can be considerably influenced by the flow oscillations. This is particularly so in the prototype system because the loop

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configuration is more complicated than the model due to the existence of two hot legs, four cold legs and a common core. Thus any flow instability has a potential of leading the system to loop to loop flow oscillations or flow excursions which may complicate the accident sequences and lead to more unreliable predictions. However, this phenomena should be studied in more detail until a comprehensive understanding can be obtained.

V. PRELIMINARY OBSERVATIONS FROM FREON-113 BOILING AND CONDENSATION LOOP

Several preliminary experiments were run using the new Freon-113 boiling and condensation loop. Because of the flexibility of the system a wide range of experimental conditions are possible, however for testing the loop simple initial and boundary conditions have been chosen. The initial conditions of the experiments for the primary side were

- the single phase liquid state throughout the loop,
- no initial flow,
- an uniform temperature throughout the loop, and
- no cooling in the simulated steam generator.

At the start of an experiment a constant power was supplied to the simulated core, then it was kept at this value until the end of the experiment. The secondary loop was kept in the shut-down mode with no cooling to the primary loop until a considerable amount of vapor accumulated in the primary side of the steam generator. Then constant subcooled liquid flow was initiated in the secondary loop to activate the heat sink to the primary side.

During the experiments several different loop-wise phenomena were observed. In the first stage, no loop-wise natural circulation of liquid was observed. The hot liquid from the core simply built up in the hot-leg by natural convections within the hot-leg. A plume of hot liquid rising in the hot-leg was visible. This stage was followed by a stable single phase natural circulation over a fairly long period. The loop wise natural circulation started when a sufficient head difference was established between the hot-leg and cold-leg return. It was observed that the flow rate was nearly constant except at the beginning and end of this stage. Both the onset and termination of the single phase natural circulation happened quickly. In the latter part of the single phase natural circulation stage, subcooled boiling was observed in the heater rod section. However, very small bubbles generated by the subcooled boiling collapsed as they rose in the vessel and no vapor accumulation was observed.

The sudden termination of the single phase natural circulation was followed by boiling in the core. This lead to the gradual build-up of a vapor space above the core. Until this vapor space reached the top of the horizontal section of the hot-leg, no vapor was transferred to the hot-leg and no loop-wise circulation was observed. The bubbles in this saturated boiling were much bigger than the ones from the subcooled boiling.

The two-phase natural circulation was established as the vapor flowed from the vessel and a sufficient void fraction was established in the hot leg. Most of the vapor came from the vapor space above the core. At the

inlet of the horizontal section, the flow pattern was quite similar to the adiabatic experiment discussed in the previous section. At the bottom of the vertical hot leg, slugging occurred due to the flow stratification in the horizontal section. However, the phase interfaces were much more unstable in the Freon experiment. The slug or cup bubbles immediately disintegrated into small bubbles. The flow regime was, therefore, mostly in churn turbulent bubbly flow even at moderate vapor fluxes. Significant turbulent motions were observed up to 2 to 3 m from the bottom. The void fraction in this section seemed to be much higher than the ones in the downstream section. Above this quite turbulent section, a typical bubbly flow was established up to the U-bend section. The flow over the U-bend section was similar to the ones in the adiabatic experiments. The flow patterns were strongly dependent on the vapor flux. The duration of the two-phase natural circulation was a function of the heat flux and the condensation rate. Without sufficient condensation in the steam generator, the vapor phase accumulated rapidly in the U-bend and upper part of the simulated steam generator. This led to the complete flow termination. However, unlike the adiabatic experiments, this was not a permanent flow termination.

It was observed that the bubbly flow in the hot leg was maintained and gradually the liquid levels in the hot leg and steam generator came down. During this stage, occurrences of sudden flashing at the upper 1/3 of the hot leg were observed. The flashing started both within the liquid and at the pressure taps. Once the flashing started, the two-phase flow regime changed dramatically from bubbly to slug flow. A slug bubble was generated by very rapid growth of a nucleated bubble. Often the bubble growth was so rapid that the flow could not be accelerated sufficiently and the motion of the slug bubble slowed down considerably. Several cycles of the slug bubble generation lead to a very much increased natural circulation rate.

Due to this increased rate, a large amount of subcooled liquid entered into the core. Because of this, the boiling in the core was completely suppressed. The vapor volume decreased considerably and the liquid level rose above the bottom of the hot leg, thus it shut down the vapor flow to the hot leg. This also led to the termination of the natural circulation. At the end of this flashing phenomena, the stagnated liquid was observed in the hot leg. The vapor phase filled the upper part of the hot leg and U-bend section. The subcooled boiling started during this stagnated period and the whole process could repeat with a period of a few minutes.

It is noted that the cooling by the secondary loop can be initiated any time during the experiment. For example, the condensation of accumulated vapor at the upper part of the steam generator can be promoted by starting the secondary loop after the termination of the two-phase natural circulation. The rapid condensation can reduce the pressure in the hot leg and may help the flashing process. However, the flashing could happen without the secondary loop circulation. The main cause of the flashing appears to be the hydrostatic pressure decrease within the hot-leg as the liquid moves up from the bottom to the top. This pressure reduction is considerable at the order of 10 psi (~69 kPa). As the saturated liquid enters into the hot-leg and rises due to natural circulation or natural convection within the hot-leg, it can be superheated. This superheating can lead to flashing at the upper part of the

hot-leg. Overall, the flow phenomena in the Freon loop are more complicated and unstable due to thermal non-equilibrium effects on phase changes.

VI. DRIFT FLUX MODEL FOR LARGE DIAMETER PIPES

In the prototype system, the diameter of the hot legs is about 90 cm and the length to diameter ratio relatively small at about 20. Thus the two-phase flow in the hot legs under natural circulation conditions can be quite different from the standard experiments. Thus the conventional two-phase flow models developed based on small diameter and large L/D system experiments may not be adequate for accurately predicting the two-phase flow phenomena in the hot legs. In view of this, the drift flux model applicable to large diameter pipes at relatively low fluxes has been developed [19]. Based on the detailed analysis of bubble behaviors the drift velocity correlations have been obtained in collaboration with a large number of experimental data. The air-water and steam-water data over the pressure range of 0.1 to 18 MPa and the diameter range of 2 to 61 cm have been used. The preliminary results [19] are summarized below.

The area averaged drift velocity $\bar{V}_{gj}$ or the relative velocity $\bar{v}_r$ is given in the standard form by

$$\bar{V}_{gj} = (1-\alpha) \bar{v}_r = (C_0 - 1) j + \langle\langle V_{gj} \rangle\rangle \quad (13)$$

where the distribution parameter C_0 represents the effect due to the phase and velocity distributions in the cross section and $\langle\langle V_{gj} \rangle\rangle$ the average local slip between phases.

For low gas flux conditions given by $j_g < 0.5 (\sigma g \Delta \rho / \rho_f^2)^{0.25}$ the standard correlations [10,11] can be used. Thus

$$\langle\langle V_{gj} \rangle\rangle = \begin{cases} \sqrt{2} \left(\frac{\sigma g \Delta \rho}{2 \rho_f} \right)^{0.25} (1-\alpha)^{1.75} & ; \text{ Bubbly} \\ \sqrt{2} \left(\frac{\sigma g \Delta \rho}{2 \rho_f} \right)^{0.25} & ; \text{ Churn-Turbulent} \\ 0.35 \sqrt{g D \Delta \rho / \rho_f} & ; \text{ Slug} \end{cases} \quad (14)$$

However the slug flow is limited [12,13] to $D \leq 40 \sqrt{\sigma / g \Delta \rho}$, and the distribution parameter is given by

$$C_0 = 1.2 - 0.2 \sqrt{\rho_g / \rho_f} \quad (15)$$

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For higher gas flux conditions given by $j_g > 0.5 (\sigma g \Delta \rho / \rho_f^2)^{0.25}$, the newly developed drift flux model has the following form for low viscous fluids.

$$\langle\langle V_{gj} \rangle\rangle = \begin{cases} 0.0019 D^{*0.809} \left(\frac{\rho_g}{\rho_f} \right)^{-0.157} N_{\mu f}^{-0.562} & \text{for } D^* \leq 30 \\ 0.030 \left(\frac{\rho_g}{\rho_f} \right)^{-0.157} N_{\mu f}^{-0.562} & \text{for } D^* > 30 \end{cases} \quad (16)$$

where $D^* = D / \sqrt{\sigma / g \Delta \rho}$ and $N_{\mu f} = \mu_f / (\rho_f \sigma \sqrt{\sigma / g \Delta \rho})^{0.5}$.

The above results are compared to a large number of experimental data. The present correlations fit to these data over wide ranges of gas flux, diameter, system pressure and liquid physical properties. The accuracy of the prediction of void and liquid fractions is within $\pm 20\%$. This is a significant improvement over the existing correlations.

VII. SUMMARY AND CONCLUSIONS

The two-phase natural circulation and flow termination in the hot-leg and U-bend have been experimentally investigated. The nitrogen-water simulation loop and Freon 113 loop were designed and constructed according to the scaling criteria developed under the program. These loops are a partial scale model of a typical B&W 2 x 4 nuclear reactor system and the main focus of the research is the natural circulation phenomena during a small break loss of coolant accident.

The phenomena were studied in detail by focussing on the simulation and scale distortion effects in terms of the hot leg U-bend phase separation, two-phase flow regimes and void distribution. The effects of parameters such as the thermal center (separator water level), loop frictional resistance, inlet section geometry and U-bend curvature on the natural circulation flow termination were experimentally analyzed. The void fraction distributions along the hot-leg were obtained from differential pressure transducers. A simple criterion for the prediction of the flow termination condition was proposed in terms of the gas superficial velocity. Important conclusions obtained from the adiabatic experiments are summarized below.

(1) In general the induced liquid natural circulation rate increased with the increasing gas flow rate. This natural circulation rate strongly depended on the loop frictional resistance and gravitational driving head.

(2) As the gas flow rate was reduced, eventually the liquid flow was terminated. This flow termination was basically governed by the hot leg void distribution and the separator water level. The frictional resistance of the loop had almost no effect on the flow termination. A higher liquid level in the separator implied higher hydraulic head, thus a smaller void fraction in the hot leg was required to sustain the natural circulation leading to a smaller gas flux at the flow termination.

(3) Temporal voiding of the U-bend section happened quasi-periodically in the slug or churn-turbulent flow regime. This was caused by the passage of large slug bubbles. Under this condition the liquid flow through the U-bend was intermittent. However, the loop natural circulation was not strongly influenced by this intermittency and quite stable. Thus the phase separation at the U-bend had no important effect on the overall natural circulation. As long as there was enough hydraulic driving head, the two-phase mixture level could recover up to the U-bend and the liquid flow was reestablished.

(4) Near the flow termination point, flow oscillations with a considerable amplitude had been observed. The period of the oscillation was between 10 to 25 sec. This indicated that the instability was related to the kinematic wave propagation. It could be stabilized both in terms of the amplitude and unstable range by increasing the single phase flow restriction.

(5) The inlet geometry effects were examined by using three different configurations, i.e., straight vertical-up, short horizontal section with a 90 degree elbow and long horizontal section. The phase separation in a horizontal section lead to the stratification. This stratified two-phase flow caused the slugging at the elbow. Thus the flow regime in the vertical section was always either in cap-bubble or slug flow. On the other hand, with the straight vertical-up inlet geometry, bubbly flow was possible. It is expected that in the prototype system, similar slugging can happen at the elbow. However, very large cap or slug bubbles are unstable [11,12], thus they should disintegrate into a large number of smaller cap and deformed bubbles.

(6) The effect of the U-bend curvature on the natural circulation was not significant. This is consistent with the second and third conclusions.

(7) A very simple criterion for predicting the flow termination point was derived from a force balance. This criterion was in good agreement with the data.

(8) There was basically no hysteresis effect between the flow termination and reestablishment.

The preliminary tests in the Freon 113 loop showed significant effects of heat transfer and phase changes. The two-phase flow regime in the hot leg was mostly churn-turbulent bubbly flow in the lower section and bubbly flow in the upper section. The horizontal slugging rarely lead to the formation of stable slug flow in contrast to the adiabatic case. This should be due to the difference in the surface tensions and the phase change. The most significant phenomenon was the flashing in the upper part of the hot leg which could induce two-phase natural circulation.

In addition to these experiments, an analytical study has been carried out to develop a two-phase flow model for large diameter pipe flow. The present correlations fit to a large number of data over wide ranges of gas flux, diameter and system pressure.

From the present experimental study, an understanding of the basic mechanism of the natural circulation termination has been established. The loop friction and thermal center play key roles in determining the natural circulation rate. The hot-leg flow regime and void distribution are also very important in predicting the phenomena. Both of these strongly depend on the inlet geometry. Several scale distortion effects in a small scale model have been pointed out. These will affect the analysis of the data from the present loop as well as the other integral facilities such as MIST and UM. In order to clarify these points, the present experimental program is continued to cover such effects as larger diameter hot leg, different fluid properties and phase changes.

NOMENCLATURE

a	Area scale
C_0	Distribution parameter for drift-flux model
D	Hydraulic diameter
f_{TP}	Two-phase friction factor
f_{SP}	Single phase friction factor
g	Gravity
j	Total volumetric flux ($j_g + j_f$)
j_g	Gas volumetric flux
K	Orifice loss coefficient
l	Axial length scale
l_w	Separator water level from bottom of loop
l_{max}	Hot leg height from bottom of loop
t	Time scale
u	Velocity scale
u_f	Liquid velocity
u_m	Mixture center of mass velocity
$\bar{v}_r$	Mean relative velocity
V	Volume scale
$\bar{v}_{gj}$	Mean drift velocity

$\langle\langle V_{gj} \rangle\rangle$	Average of local drift velocity
x	Vapor quality
z	Axial coordinate

Greek Symbols

α	Void fraction
$\Delta\rho$	Density difference ($\rho_f - \rho_g$)
ρ_f	Liquid density
ρ_g	Gas density
ρ_m	Mixture density
σ	Surface tension

Subscripts

$()_m$	Model value
$()_p$	Prototype value
$()_R$	Ratio of model to prototype values

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REFERENCES

1. Ishii, M. and Kataoka, I., "Similarity Analysis and Scaling Criteria for LWR's under Single-phase and Two-phase Natural Circulation, NUREG/CR-3267, ANL-83-32 (1983).
2. Ishii, M. and Kataoka, I., "Scaling Laws for Thermal-hydraulic System under Single Phase and Two-phase Natural Circulation," Nucl. Eng. & Design, Vol. 81, pp. 411-425 (1984).
3. Kocamustafaogullari, G. and Ishii, M., "Scaling Criteria for Two-phase Flow Natural and Forced Convection Loop and Their Application to Conceptual 2 x 4 Simulation Loop Design, NUREG/CR-3420, ANL-83-61 (1983).
4. Kocamustafaogullari, G. and Ishii, M., "Scaling Criteria for Two-phase Flow Loop and Their Application to Conceptual 2 x 4 Simulation Loop Design," Nucl. Tech., Vol. 65, pp. 146-160 (1984).

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5. Carter, H. R., "MIST Facility Status," Proc. 13th Water Reactor Safety Research Information Meeting, NUREG/CP-0072, Vol. 4, pp. 83-100 (1985).
6. Wang, Z., Popp, M., DiMarzo, M. et al., "University of Maryland Test Facility Results," Proc. 13th Water Reactor Safety Research Information Meeting, NUREG/CP-0072, Vol. 4, pp. 47-82 (1985).
7. Ishii, M. and Jones, O., Jr., "Derivation and Application of Scaling Criteria for Two-Phase Flows," Two-Phase Flows and Heat Transfer, proc. Nato Advanced Study Institute, Istanbul, Turkey, Vol. 1, p. 163 (1976).
8. Ishii, M. and Zuber, N., "Thermally Induced Flow Instabilities in Two-Phase Mixture," 4th Intl. Heat Trans. Conf., Paris, paper B5.11 (1970).
9. Zuber, N., "Problems in Modeling of Small Break LOCA," in Heat Trans. in Nucl. Reactor Safety, edited by S. G. Bankoff and N. H. Afgan, p. 3, Hemisphere Pub. Corp. (1983).
10. Zuber, N. and Findlay, J. A., "Average Volumetric Concentration in Two-Phase Flow Systems," J. Heat Trans., Vol. 87, p. 453 (1965).
11. Ishii, M., "One-Dimensional Drift-Flux Model and Constitutive Equations for Relative Motion between Phases in Various Two-Phase Flow Regimes," Argonne National Laboratory Report ANL-77-47 (1977).
12. Kocamustafaogullari, G. and Ishii, M., "Unified Theory for Predicting Maximum Fluid Particle Size for Drops and Bubbles," NUREG/CR-4028, ANL-84-67 (1984).
13. Kocamustafaogullari, G. and Ishii, M., "Maximum Fluid Particle Size for Bubbles and Drops," ASME Winter Annual Meeting, Miami Beach, Nov. 17-22, Proc. Fundamental Aspects of Gas-Liquid Flow, FED-Vol. 29, pp. 99-109 (1985).
14. Kim, S. B. and Ishii, M., "Flow Visualization Experiment on Hot-Leg U-Bend Two-Phase Natural Circulation Phenomena," NUREG/CR-4621, ANL-86-27 (1986).
15. Hsu, J. T. and Ishii, M., "Experimental Study on Two-phase Natural Circulation and Flow Termination in a Loop," NUREG/CR-4682, ANL-86-32 (1986).
16. Mishima, K. and Ishii, M., "Flow Regime Transition Criteria for Upward Two-phase Flow in Vertical Tubes," Intl. J. Heat Mass Trans., Vol. 27, pp. 723-737 (1984).
17. Ishii, M. and Zuber, N., "Thermally Induced Flow Instabilities in Two-phase Mixtures," 4th Intl. Heat Trans. Conf., No. B5.11, Paris (1970).
18. Ishii, M., "Study on Flow Instabilities in Two-phase Mixture," ANL-76-23 (1976).

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19. Kataoka, I. and Ishii, M., "Prediction of Pool Void Fraction by New Drift Flux Correlation," NUREG/CR-4657, ANL-86-29 (1986).

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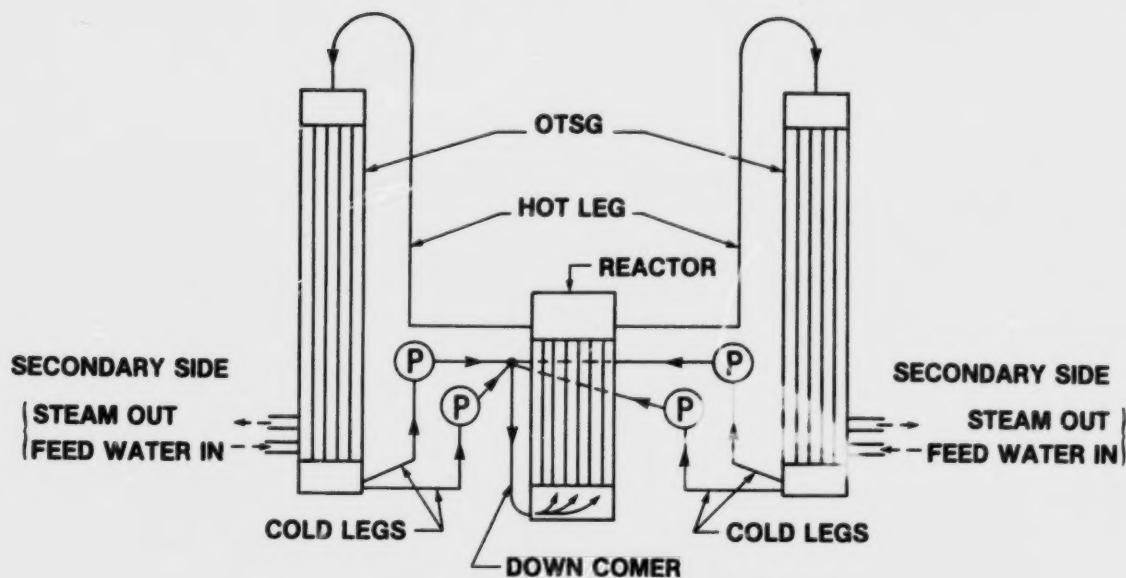


Fig. 1. Schematic of B&W 2x4 Loop Nuclear Reactor

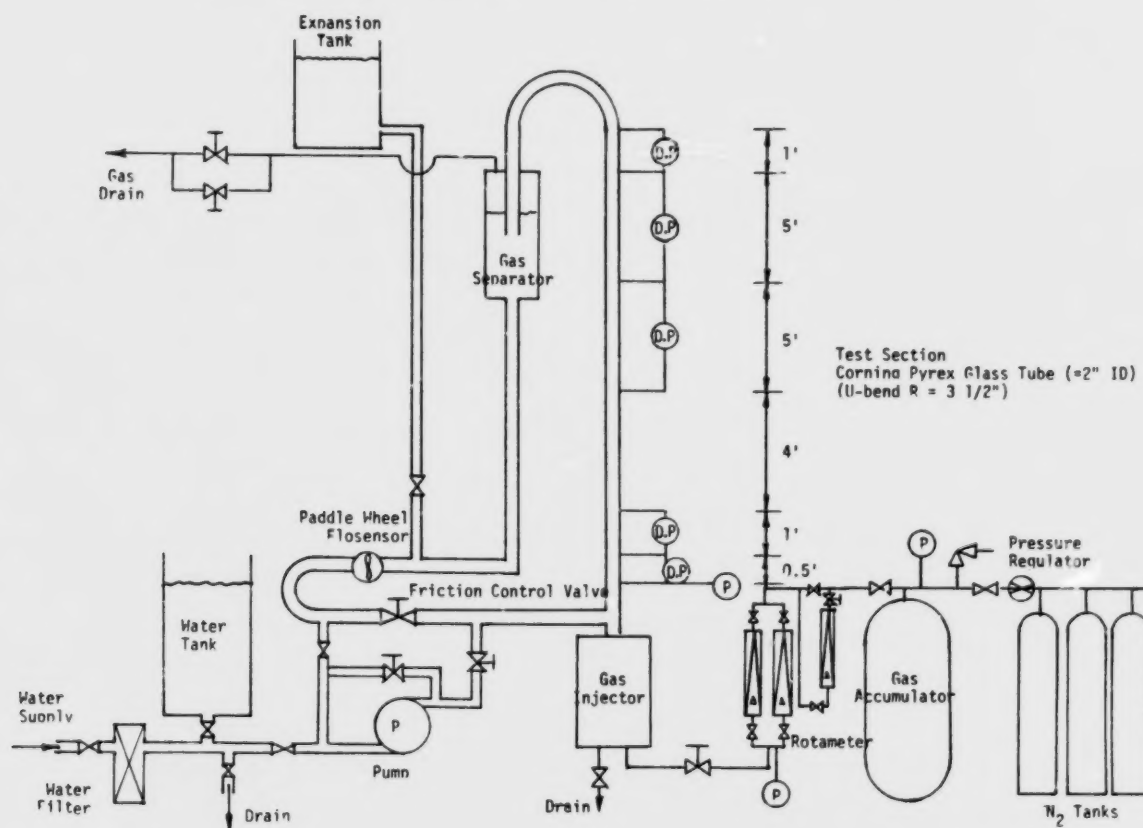


Fig. 2. Schematics of Hot-leg U-bend Simulation Loop for Nitrogen Gas-water Experiments

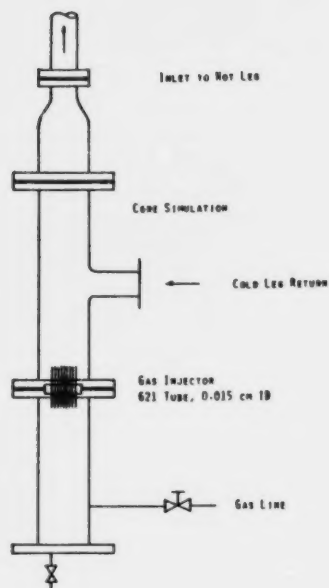


Fig. 3. Hot-leg Inlet of Vertically Straight Geometry without Horizontal Hot-leg Section

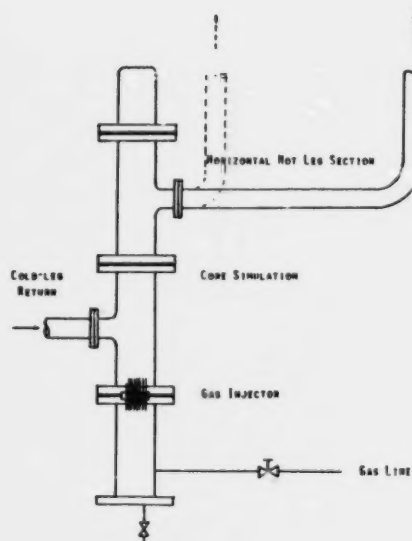


Fig. 4. Hot-leg Inlet with Horizontal Section Simulation Reactor Vessel Exit Geometry (short and long horizontal sections shown)

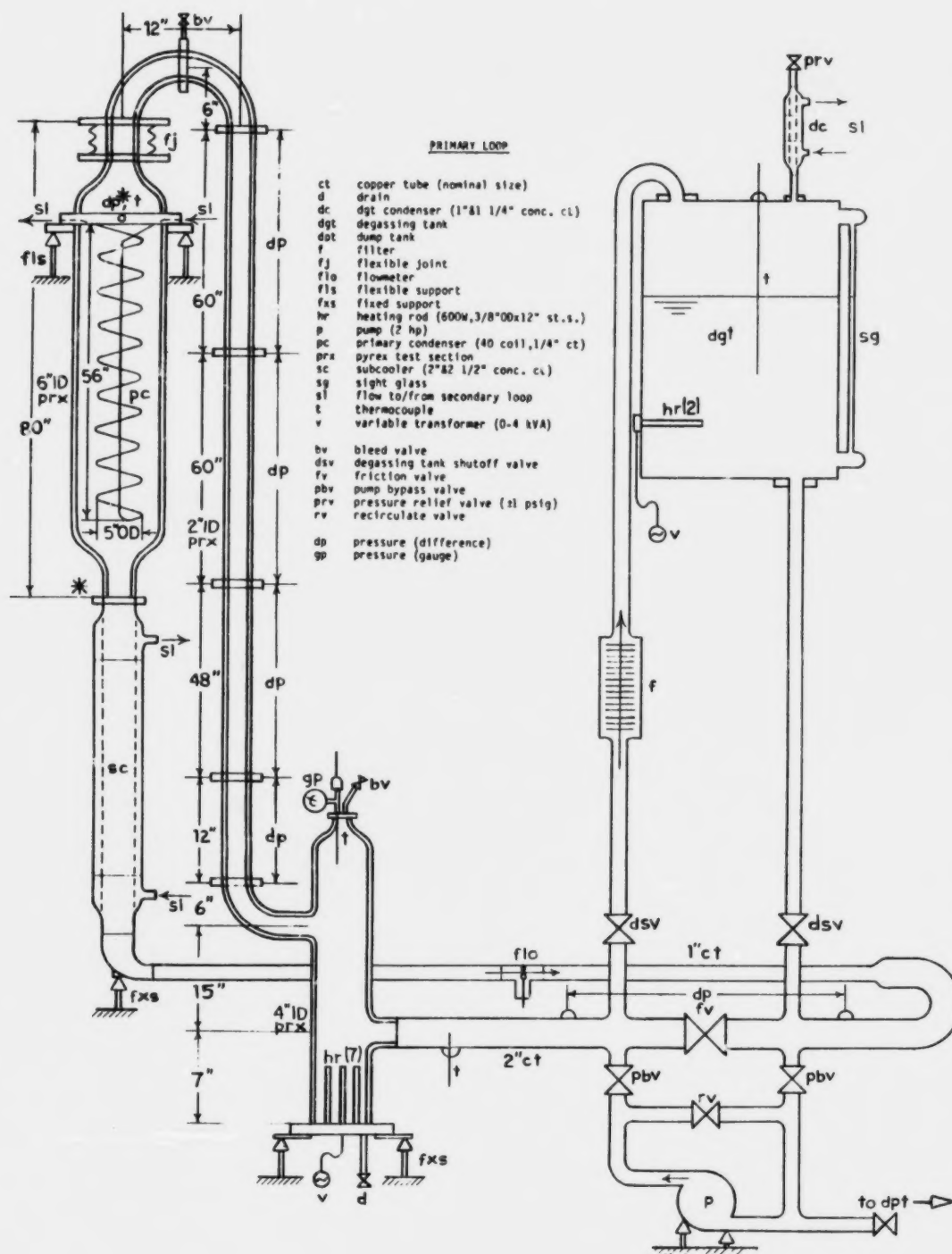


Fig. 5. Schematic of Freon 113 Boiling and Condensation Loop for Hot-leg U-bend Natural Circulation Study

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THERMAL MIXING TESTS IN A SEMIANNULAR
DOWNCOMER WITH INTERACTING FLOWS FROM COLD LEGS

by

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IMATRAN VOIMA OY
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ABSTRACT

This paper describes the test facility and test program for studying thermal mixing of high-pressure injection (HPI) water in the two-fifths scale model of three cold legs, semiannular downcomer and lower plenum of a pressurized water reactor.

The test facility was originally designed to model the Finnish Loviisa plant but it was redesigned and modified for this test program. The facility can be operated at atmospheric pressure with loop and HPI flows from different cold legs in the area of interest to pressurized thermal shock. The full buoyancy effect was induced by salt addition and the HPI temperature was used as a tracer.

The test matrix consists of 20 tests. The varied parameters were flow rates and the number and configuration of cold legs with HPI and loop flows. Four tests were done with decreasing loop flow temperature to simulate primary flows during steam line breaks.

INTRODUCTION

An experimental facility was constructed at the Hydraulic Laboratory of Imatran Voima Oy (IVO) 1983 to study thermal mixing of cold high-pressure injection (HPI) water with hot primary coolant in a pressurized water reactor (PWR) during postulated overcooling accidents. The facility was built as a two-fifths scale model of the Loviisa VVER-440 reactor. An experimental program of about 50 tests was performed during the same year /1,2/.

After completion of the first test series, IVO and the US Nuclear Regulatory Commission (NRC) made an agreement on Pressurized Thermal Shock (PTS) information exchange, and a new test series was decided to be performed with a reconstructed facility. Test conditions and modifications to the facility were defined by NRC. In this report the modified test facility and test series will be presented.

TEST FACILITY

The basic experimental facility contains a half of the circumference of the reactor downcomer made of acrylic plastic. The downcomer is semiannular in shape.

The three cold legs were rebuilt for these tests. Their position with respect to the downcomer was changed to be asymmetric so that adjoining pipes form angles of 30° and 60° . All the three cold legs are similar with regard to their geometry. They are straight in shape and have a diameter d_{CL} of 194 mm. Figure 1 gives a schematic view of the facility.

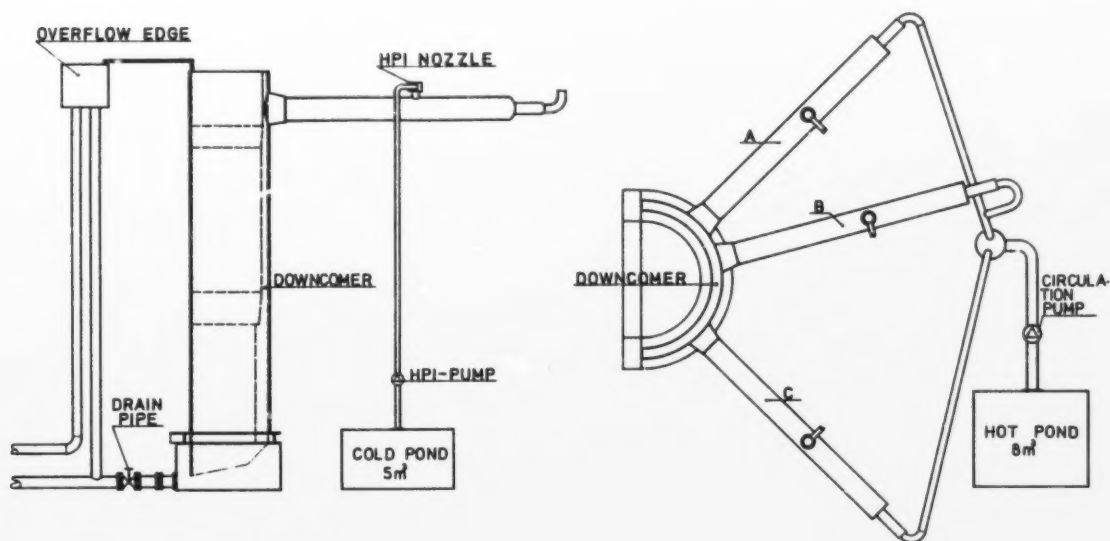


Figure 1. Schematic of the thermal mixing facility.

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A required HPI flow can be directed to each cold leg. The HPI nozzles are placed at a distance of seven times the cold leg diameter measured from the downcomer wall. The diameter of the nozzles is 1/3 of the primary pipe diameter ($d_{HPI} = 65 \text{ mm}$).

HPI water was injected from top of the cold leg perpendicular to the pipe centre line. Flow equalizers were fitted in HPI lines before HPI nozzles to smooth the flow.

The perforated plate was also removed for these tests from the lower plenum. In previous tests the primary coolant flowed through this plate from the downcomer to the lower plenum.

The purpose of the test series was to measure mixing of the hot primary water and the cold HPI water in this particular geometry. A special task was to confirm good mixing in the downcomer with interacting flows from different cold legs. For this, the water temperature in different parts of the facility was recorded, and especially in the downcomer. Inaccuracy of the used NiCr-Ni thermocouples was $\pm 0,5^\circ \text{C}$ with respect to reference detectors.

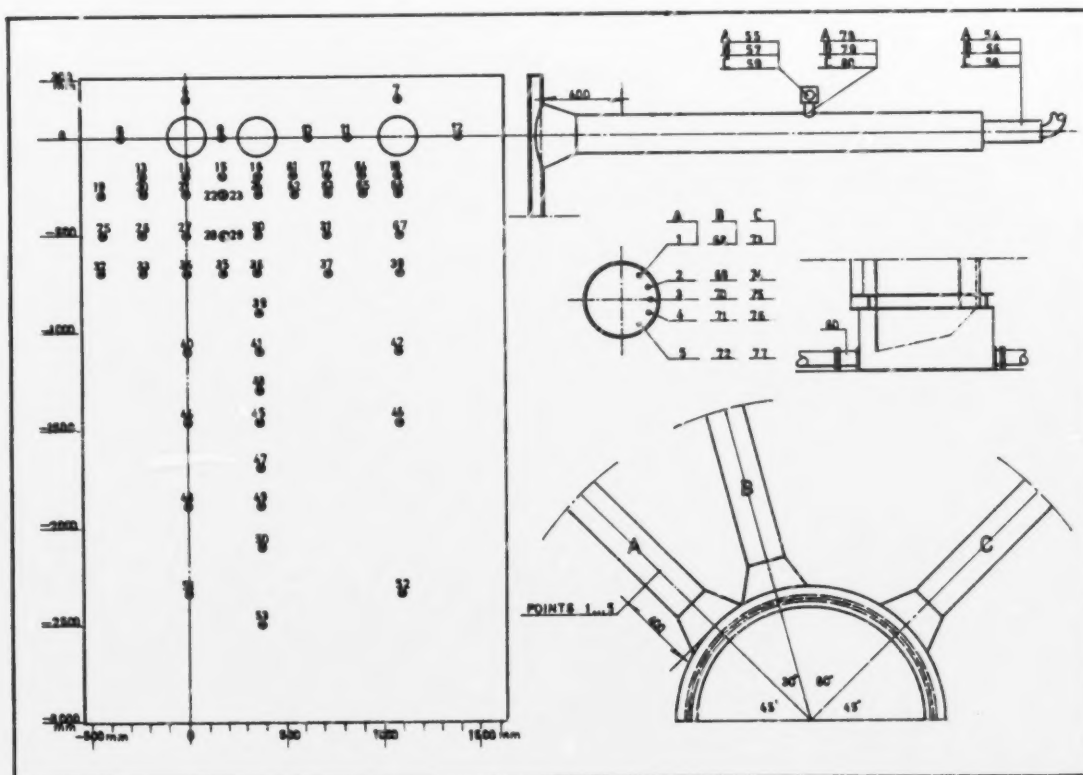


Figure 2. Thermocouple location diagram.

The temperature measurement map shows the locations of 80 thermocouples (Figure 2). Most of the thermocouples were placed in the downcomer at a distance of about 35 mm from the vessel wall.

The measuring period during which the measuring system read the points once lasted about 2.5 s, when the thermocouples totalled 80. The duration of the test could be freely chosen. The duration of tests is about ten minutes, except for cases with HPI flow only where longer runs were performed.

The structure of the model enables the primary water temperature to be raised by about 75°. The HPI water temperature in the cold tank could be maintained at about 17° to 20°.

The density difference for full buoyancy effect was induced by dissolving salt (CaCl_2) in HPI water. The relative density difference used in the tests was

$$\Delta \rho / \rho = \frac{\rho_{\text{HPI}} - \rho_L}{\rho_{\text{HPI}}} = 0.167$$

where ρ_{HPI} and ρ_L are the densities of HPI and loop flow, respectively.

TEST PROGRAM

In the tests the loop flow rate (Q_L) and the HPI flow rate (Q_{HPI}) were varied. The test conditions can be divided into five groups:

- Group 1: Tests in which interaction of plumes caused by HPI into two cold legs was studied. The loop flow was introduced to one cold leg only.
- Group 2: Tests in which HPI was injected into one cold leg only, and loop flow was introduced in another cold leg.
- Group 3: Tests in which loop flow was 0 dm³/s in all cold legs.
- Group 4: Tests in which the loop flow temperature was lowered stepwise during the measurements from 75 °C to 25 °C.
- Group 5: Tests in which the plume interaction was studied when both HPI and loop flow were introduced to two cold legs.

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In addition, tests were made in which flow behaviour and mixing in the downcomer were recorded by photographing.

Table 1 gives all the test conditions and the numbering of tests related to them.

The modified Froude-number $Fr_{CL,HPI}$ for the HPI flow rate Q_{HPI} in regard to the cold leg is defined by relation

$$Fr_{CL,HPI} = \frac{Q_{HPI}/A_{CL}}{(g D_{CL} \Delta \rho / \rho)^{1/2}}$$

where g is acceleration due to gravity, and A_{CL} and D_{CL} are the cold leg cross section and diameter, respectively.

TEST	GROUP	$Q_{HPI} [dm^3/s]$			$Q_L [dm^3/s]$		
		A	B	C	A	B	C
101	1	.73	.73	0	0	0	7.3
102	2	0	.42	0	0	0	4.2
103	1	1.48	1.48	0	0	0	10.4
104	1	1.48	0	1.48	0	10.4	0
105	1	0	1.48	1.48	10.4	0	0
106	3	.42	0	.42	0	0	0
107	2	1.48	0	0	0	0	14.8
108	1	0	.42	.42	4.2	0	0
109	2	1.04	0	0	0	0	4.2
110	2	.42	0	0	0	2.1	0
111	3	.42	.42	.42	0	0	0
112	3	1.04	0	1.04	0	0	0
113	3	0	1.48	1.48	0	0	0
114	4	1.04	0	0	0	0	4.2
115	4	0	.42	0	0	0	.84
117	4	0	.42	0	0	0	4.2
118	4	.42	0	0	0	0	4.2
119	5	1.04	0	1.04	2.08	0	2.08
120	5	0	1.48	1.48	0	2.96	2.96

Table 1

Test matrix.

Flow behaviour in the downcomer was visualized by adding dye to HPI water. Some of the tests were then recorded by photographing. However, visualization by photographs can be applied only in case of pure loop flow in some loop(s) as all the downcomer would be dyed rather rapidly. To avoid these difficulties downcomer flow distributions were recorded in ten tests by manual drawing (tests # 105, 108, 109, 112, 113, 115, 116, 118, 119 and 120). All these drawings and photographs are given in the test report /3/. In Figures 3 through 7 examples of drawings are given.

Some thermocouple readings have been added to each drawing to better illustrate the situation. When flow distributions are changing with time, Figures are shown for different periods.

Figure 3 (test # 105) shows the typical behaviour of the tests of Group 1: two HPI plumes tend to join and thermal stratification is seen in the downcomer because of the loop flow in the third cold leg. There is no flow through the surface of stratified layer. Hot water flows vertically in the nozzle area and mixes into vertical HPI-plumes.

Figures 4 a and b illustrate the corresponding behavior of the tests in Group 2. One vertical plume is formed and thermal stratification is again seen in the downcomer.

Figures 5 a and b show the mixing and interaction of two HPI plumes without any loop flow (Group 3). The plumes show a tendency to join but most of the time they are unstable. The temperature of all the downcomer region decays exponentially and no stratification effects are seen in the downcomer.

Figures 6 a to d show a test from Group 4. During the test HPI was introduced to one cold leg. Loop flow temperature in another cold leg was decreased gradually. The test was aimed to simulate the situation during steam line break. Again the tendency of plumes to unstable behaviour is seen.

The situation where both loop flow and HPI flow takes place in the same loop is shown by Figure 7. Mixing in the cold leg is very good but stratification is still present. This is illustrated by the cold leg flow distribution in the Figure 7. This test belongs to Group 5.

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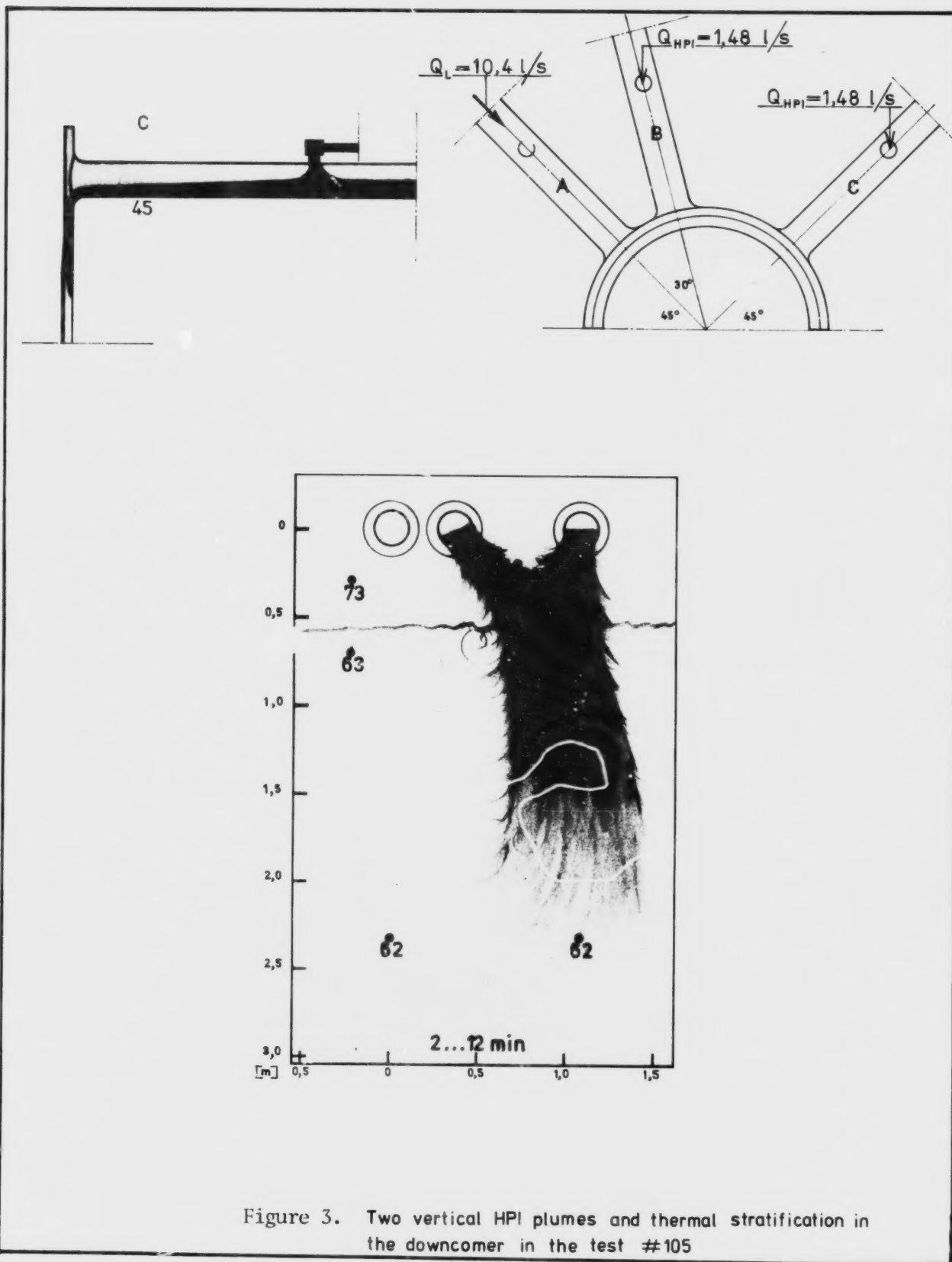


Figure 3. Two vertical HPI plumes and thermal stratification in the downcomer in the test #105

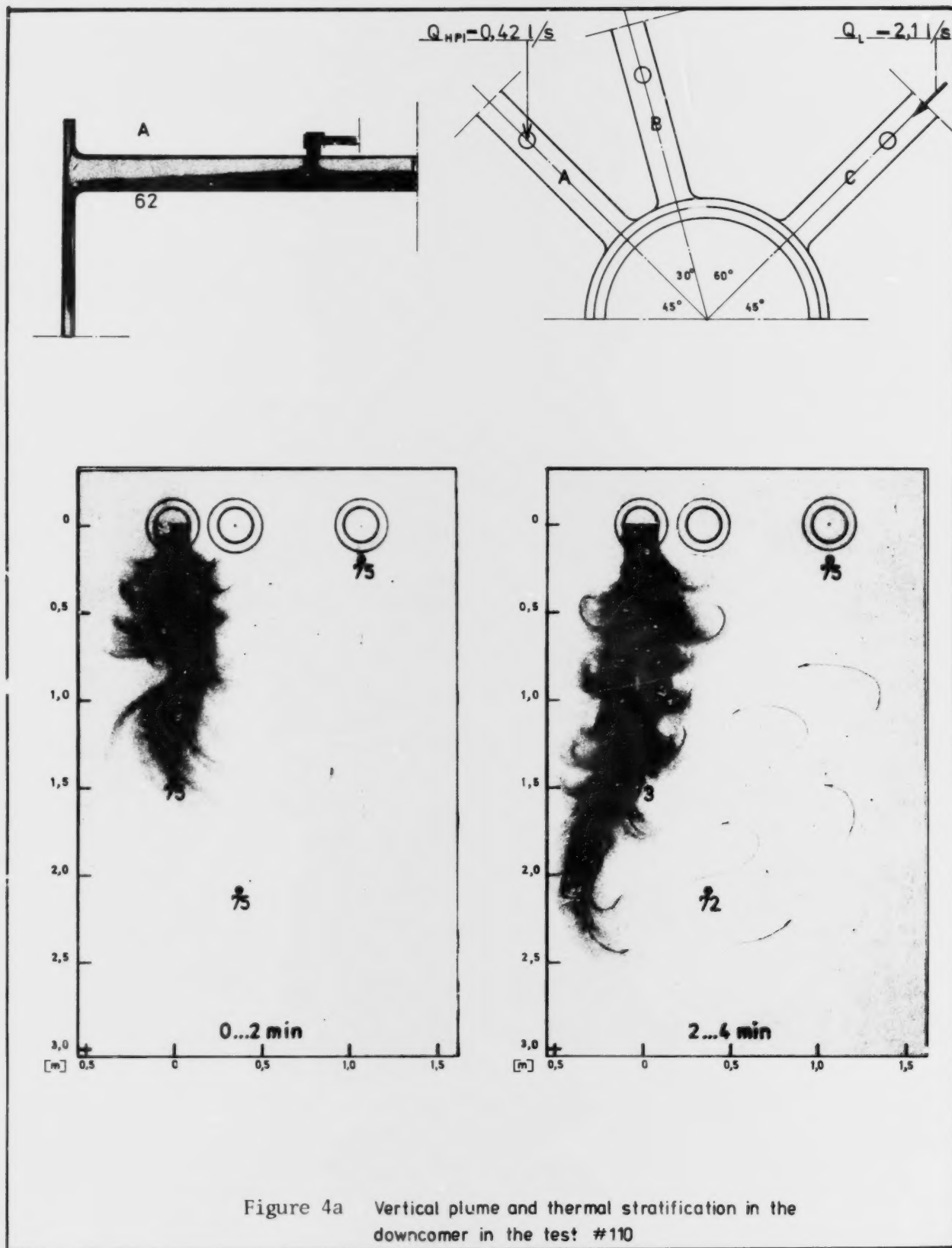
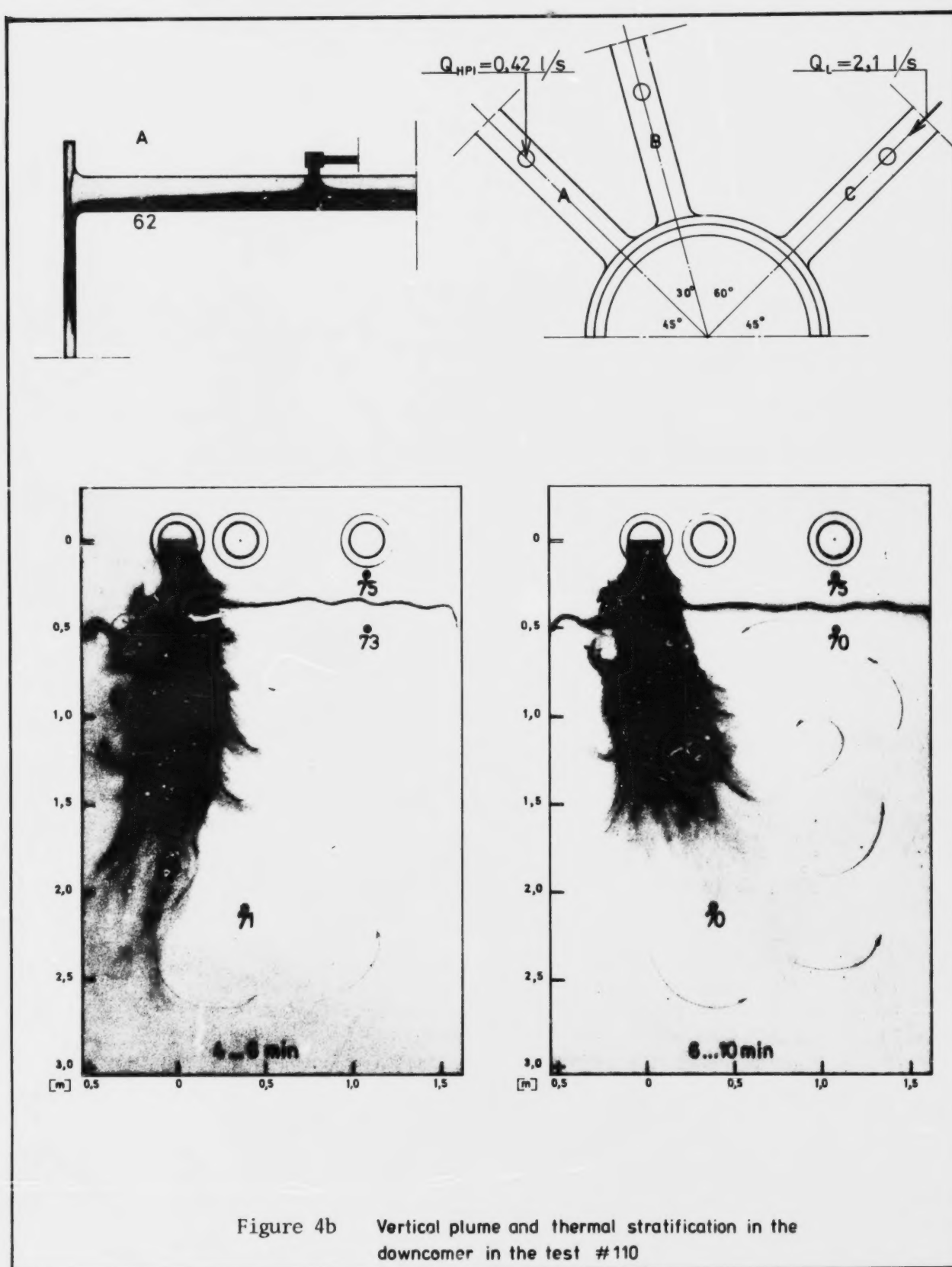
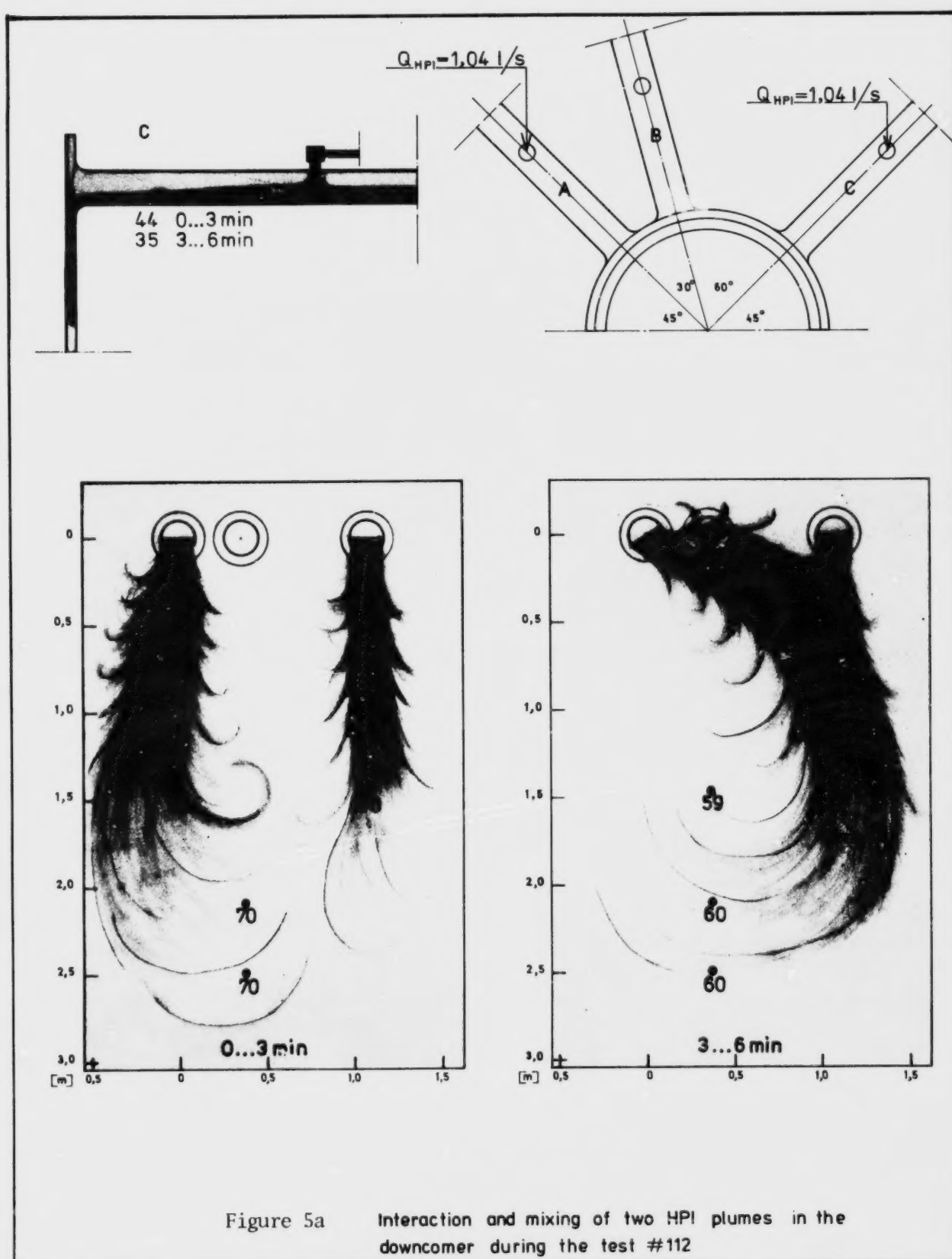
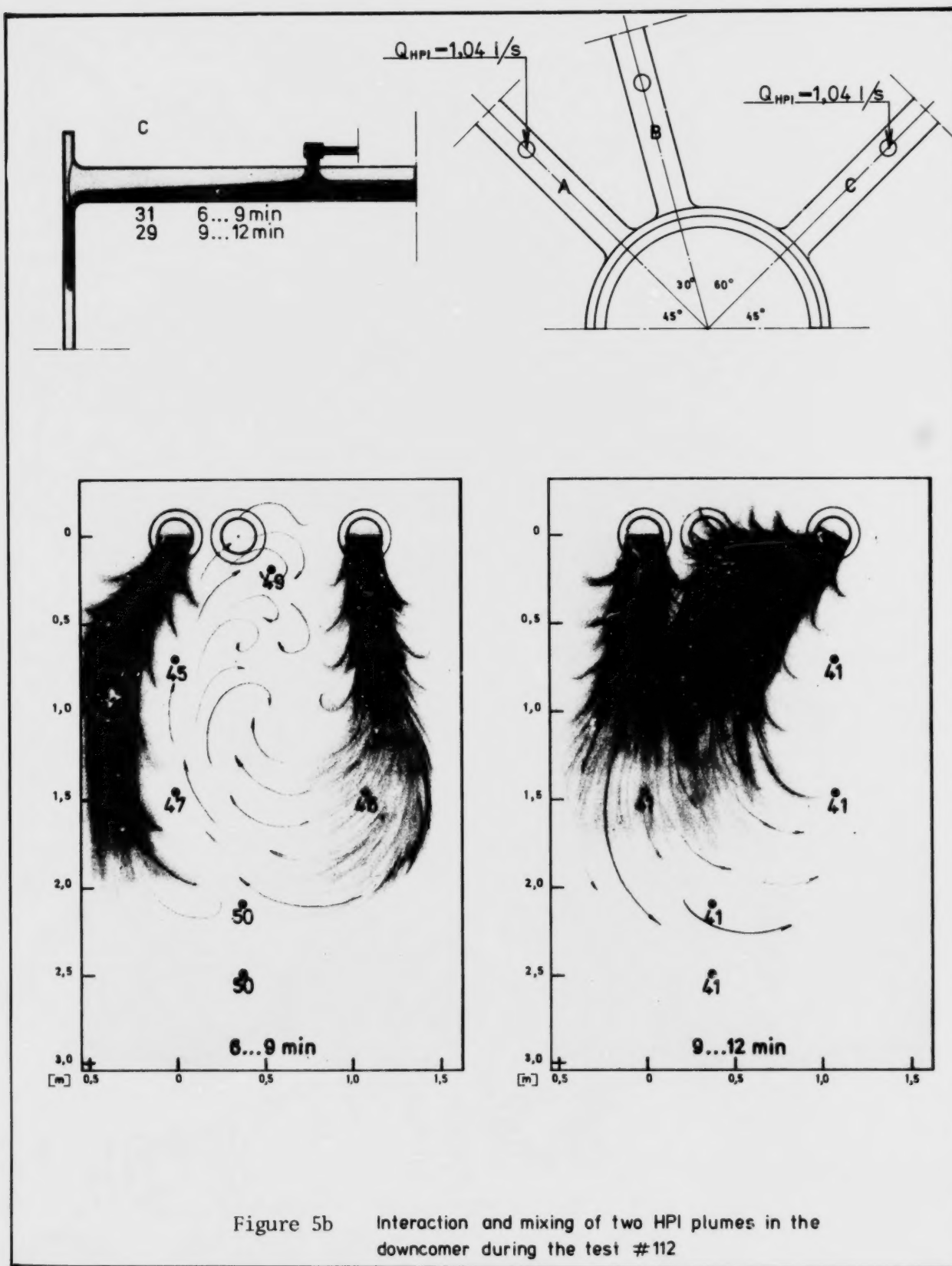


Figure 4a Vertical plume and thermal stratification in the downcomer in the test #110

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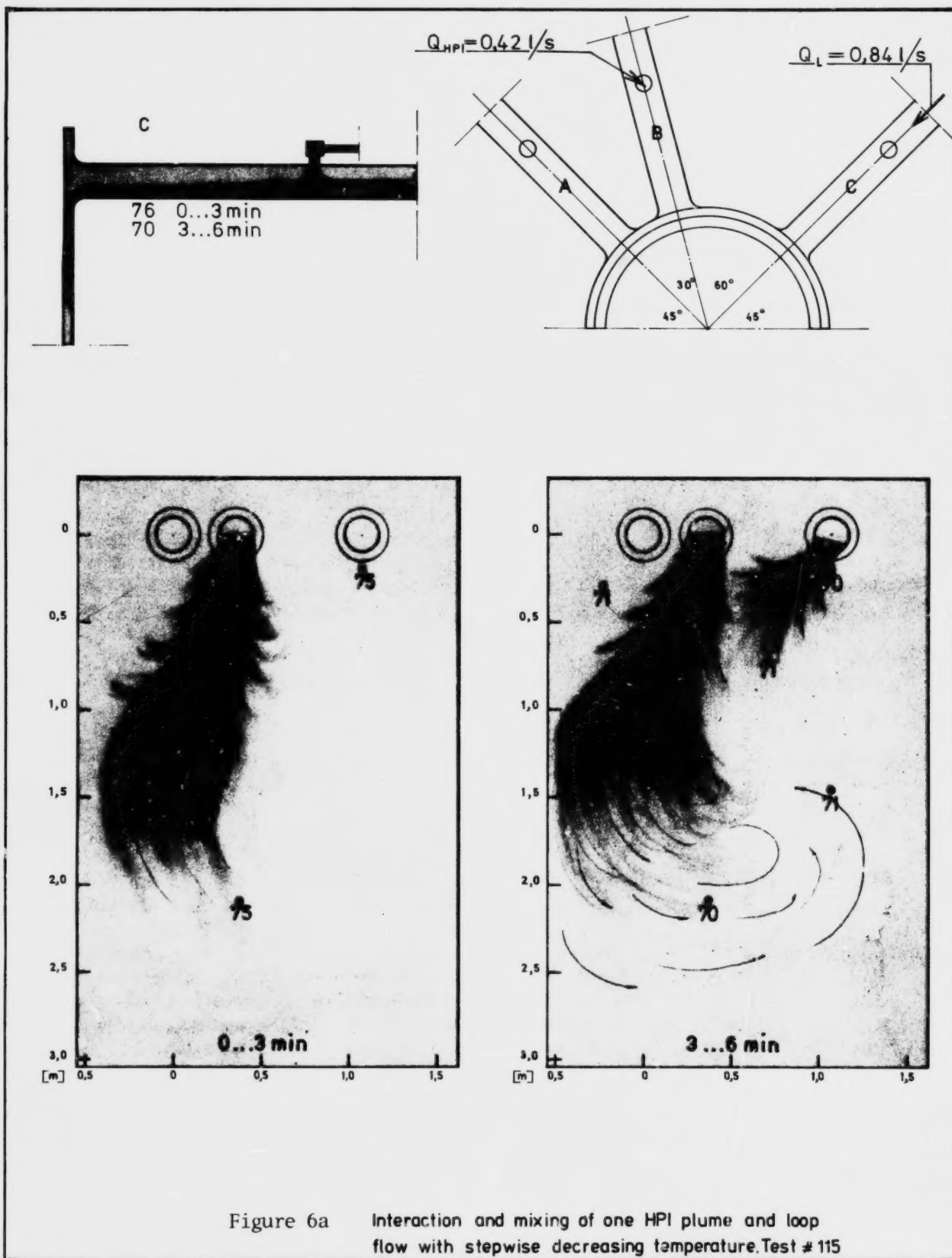
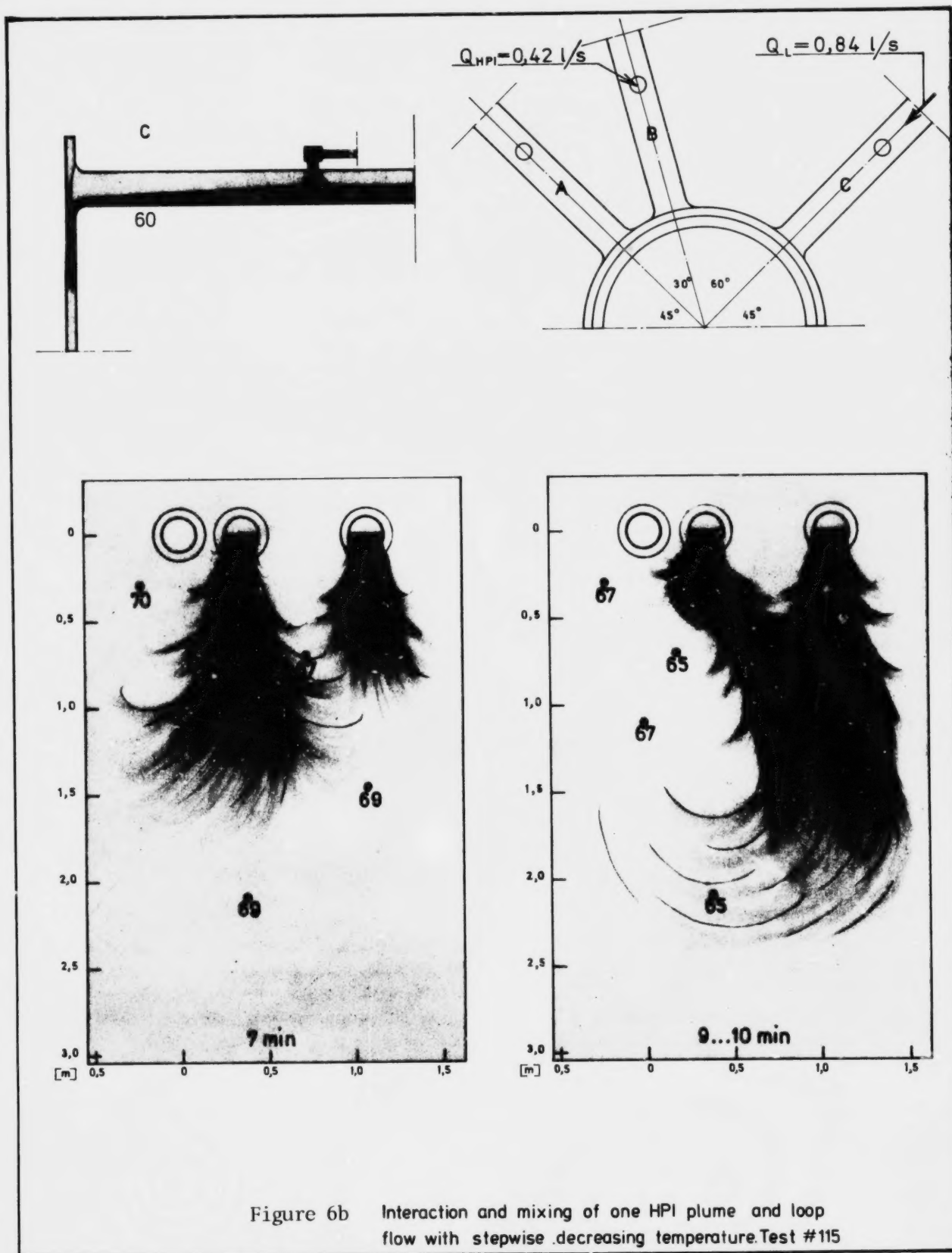


Figure 6a Interaction and mixing of one HPI plume and loop flow with stepwise decreasing temperature. Test # 115



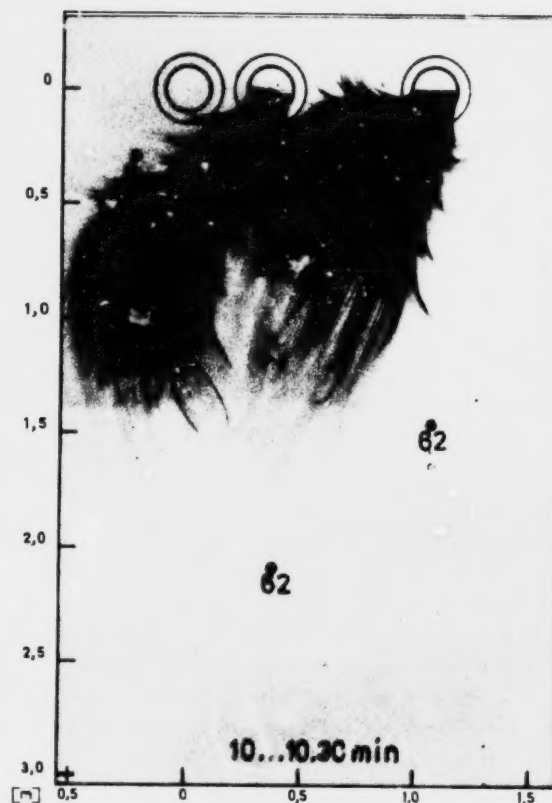
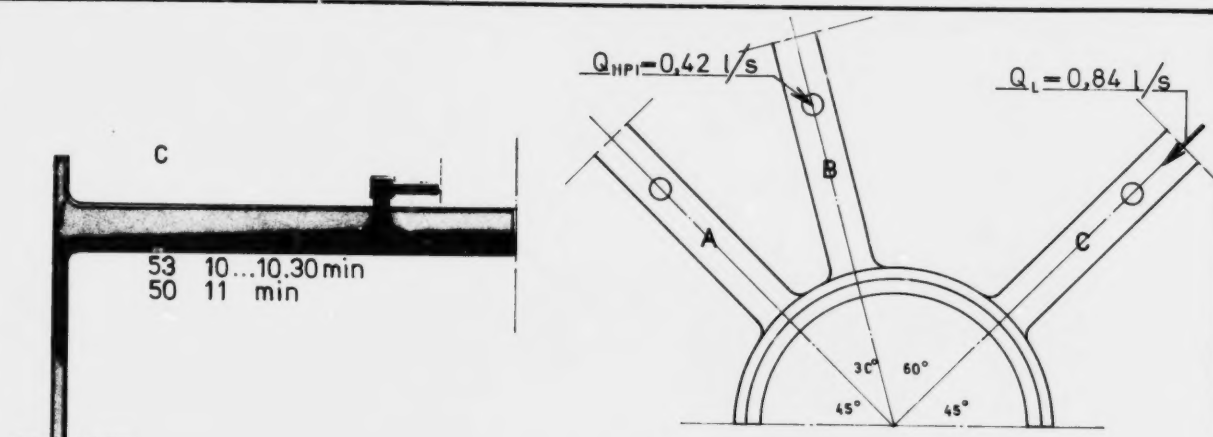
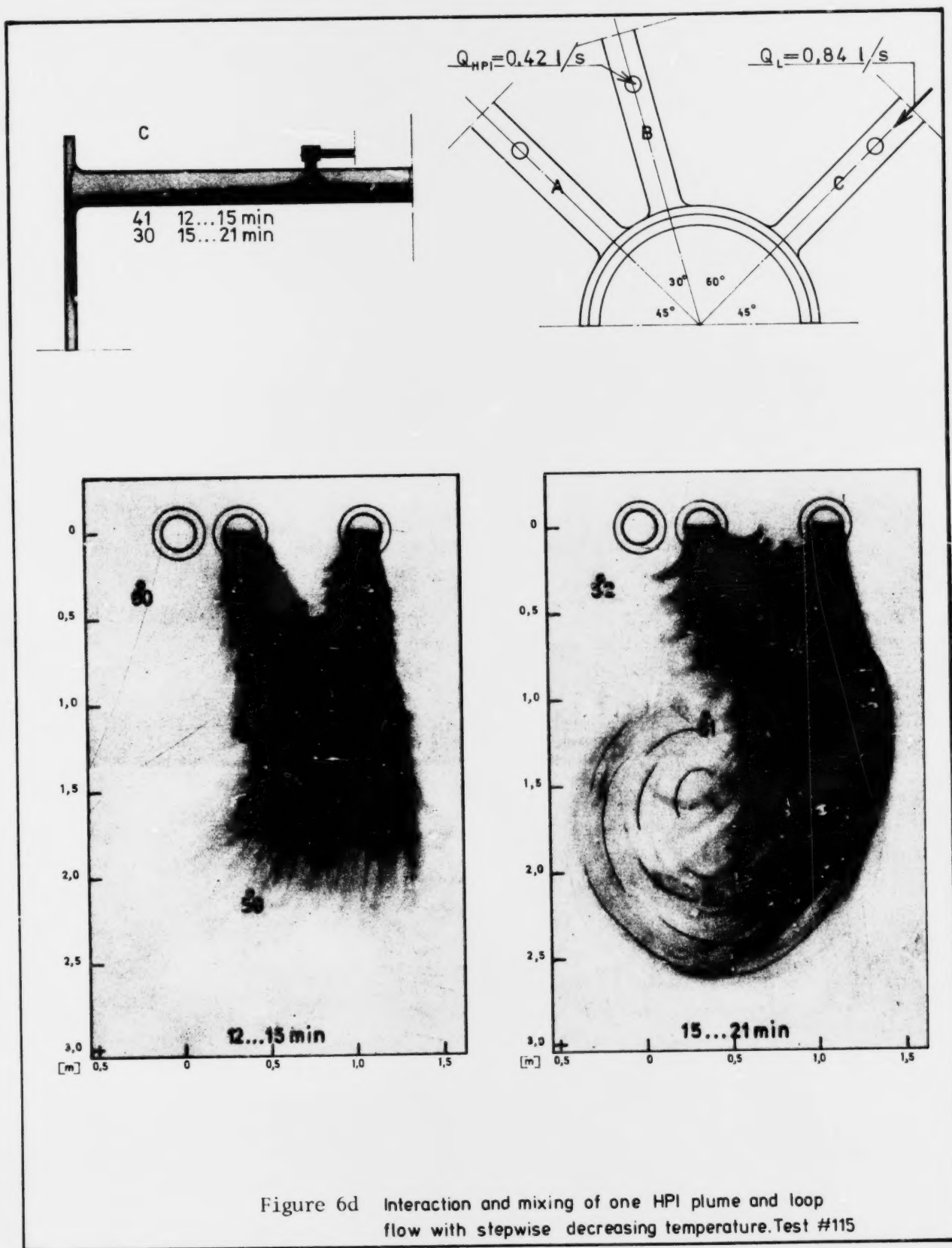


Figure 6c Interaction and mixing of one HPI plume and loop flow with stepwise decreasing temperature. Test #115



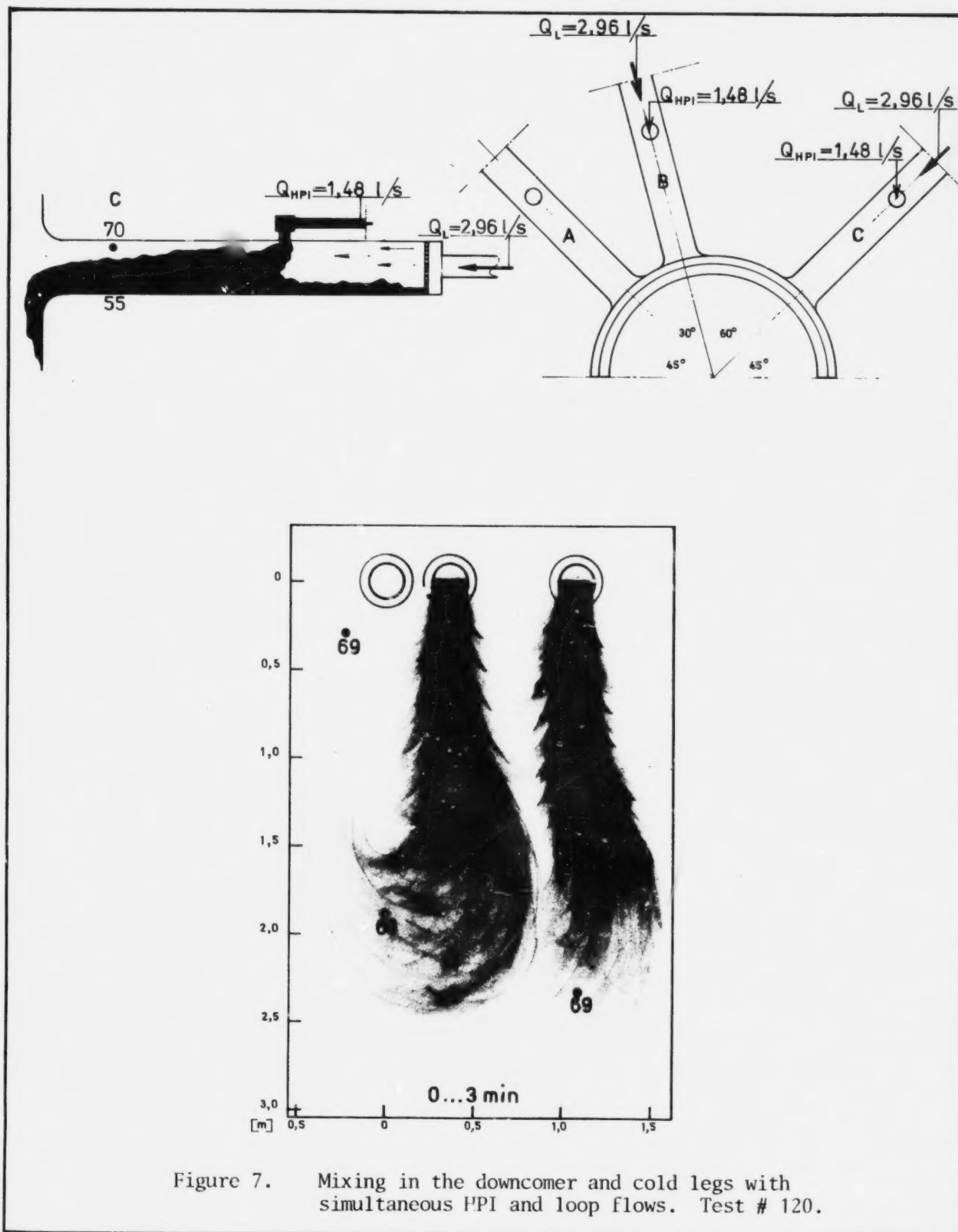


Figure 7. Mixing in the downcomer and cold legs with simultaneous HPI and loop flows. Test # 120.

CODE CALCULATIONS

These tests add multiloop operation to the data base of different thermal mixing programs. Test results have been utilized to assess the REMIX computer code /4/ developed for thermal mixing calculations during potential pressurized thermal shock (PTS) events. The assessment work of the REMIX is done in the first instance by code developers themselves (Theofanous et al, University of California, Santa Barbara).

In Finland the REMIX code has been utilized in the integrated PTS study of the Loviisa reactor pressure vessel /5/ to calculate downcomer temperature during flow stagnation. Validating the code for these calculations has necessitated an own verification program. As an example of comparison of the calculated temperatures to experimental data has been given in Figures 8 and 9 for the tests # 111 and 112. In these tests, the loop flow rate has been zero in all three loops (complete stagnation). The situation is approximated with symmetrical behaviour and calculated as HPI fed to a single loop with corresponding fraction of the downcomer.

Compared quantities are

- temperature of cold stratified layer in a cold leg (thermocouples T4 and T5)
- planar plume temperature in a distance of three cold leg diameters below the cold leg nozzle (thermocouple T34)
- plume temperature in circumferential weld area referred to Loviisa reactor pressure vessel (thermocouple T44)
- well mixed temperature in the facility outlet (thermocouple T60)

Comparison shows a good agreement of calculated results with experiments. This indicates the suitability of single loop approximation for complete stagnation cases.

CONCLUSIONS

The performed tests confirm good mixing in a semiannular downcomer with interacting buoyant plumes. Interacting plumes often show a tendency to join. When loop flow rates are low compared to HPI flow, interacting plumes are unstable, thus increasing turbulent mixing. In case of unstable behavior, plumes are not necessarily vertical, but they can turn to the side or even upwards depending on the downcomer recirculation flows, and on the fact how strong plumes are.

These IVO-USNRC mixing tests supplement the data base from



FLUID AND THERMAL MIXING TESTS

Test : 111 $Q = 0.0 \text{ dm}^3/\text{s}$ $Q_{HPLA} = .42 \text{ dm}^3/\text{s}$
 $\rho = 978. \text{ kg/m}^3$ $Q_{HPLB} = .42 \text{ dm}^3/\text{s}$
 $Q_{HPLC} = .42 \text{ dm}^3/\text{s}$
 $\rho = 1166. \text{ kg/m}^3$

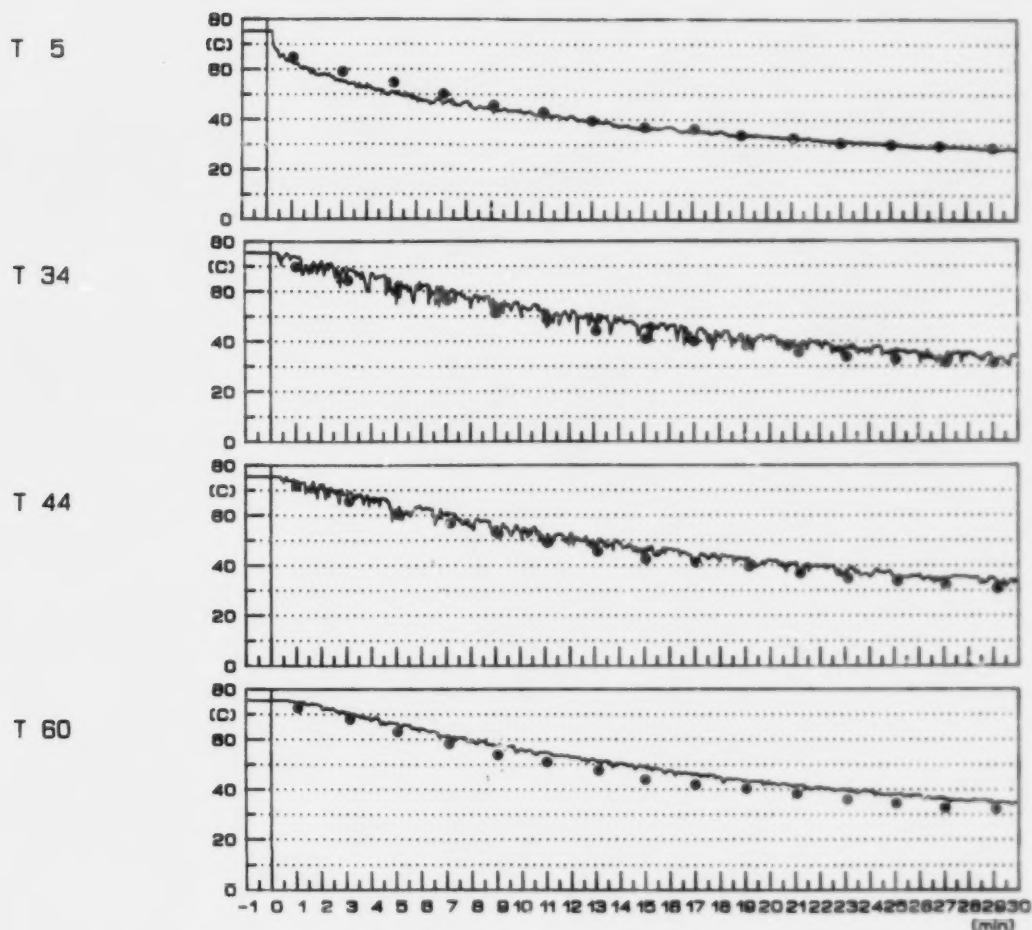


Figure 8. Calculated temperatures versus data for the test # 111.
• REMIX calculation
- experiments

T5 cold stream temperature in a cold leg
T34 plume temperature 600 mm below the cold leg nozzle
T44 plume temperature 1360 mm below the cold leg nozzle
T60 well mixed temperature

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FLUID AND THERMAL MIXING TESTS

Test : 112 $Q = 0.0 \text{ dm}^3/\text{s}$ $Q_{HPLA} = 1.04 \text{ dm}^3/\text{s}$
 $\rho = 979. \text{ kg/m}^3$ $Q_{HPLB} = 0.00 \text{ dm}^3/\text{s}$
 $Q_{HPLC} = 1.04 \text{ dm}^3/\text{s}$
 $\rho = 1166. \text{ kg/m}^3$

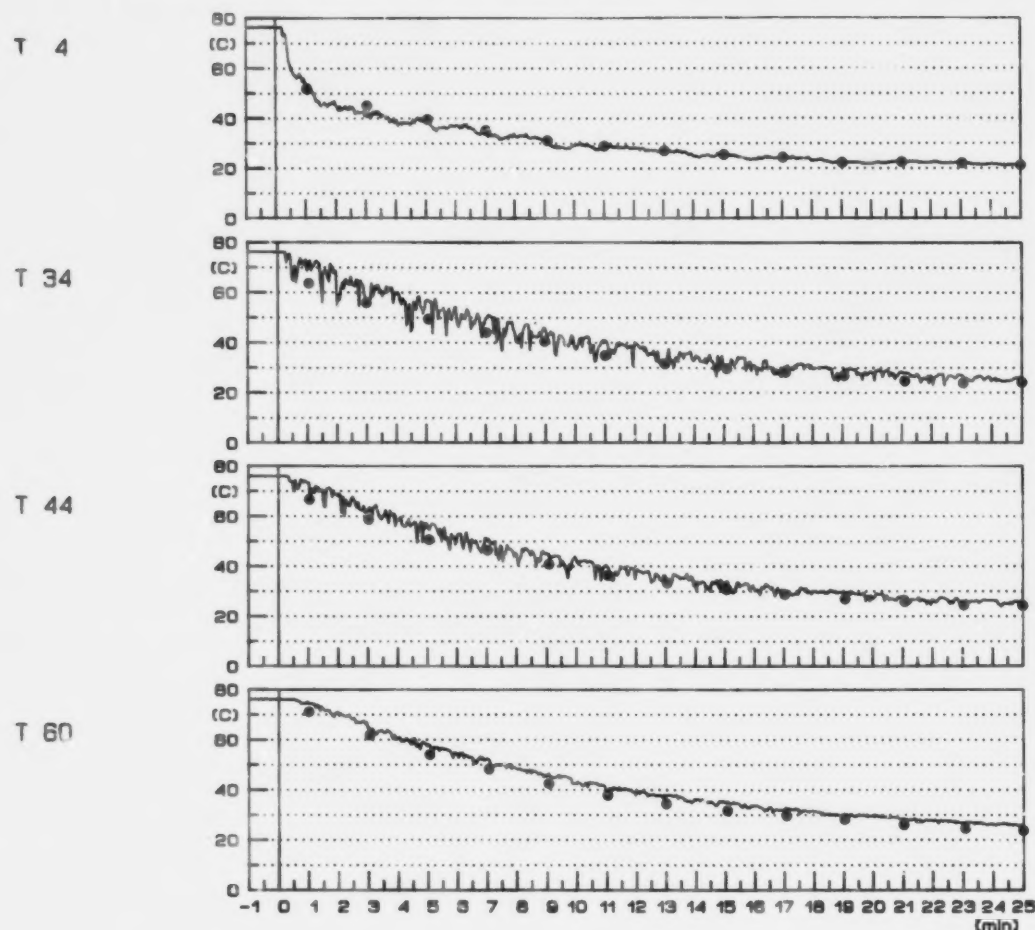


Figure 9. Calculated temperatures versus data for the test # 112.

• REMIX calculation
- experiments

T4 cold stream temperature in a cold leg
T34 plume temperature 600 mm below the cold leg nozzle
T44 plume temperature 1360 mm below the cold leg nozzle
T60 well mixed temperature

different thermal mixing programs with multiloop operation experiments. The main use of the test results is assessing the REMIX-code /4/ developed for thermal mixing calculations during potential PTS events.

The results of REMIX-code verification calculations performed in Imatran Voima Oy show a good agreement with the test data.

ACKNOWLEDGEMENTS

This work was performed as part of the mutual agreement on the PTS information exchange between the U.S. Nuclear Regulatory Commission and Imatran Voima Oy. The author of this paper would like to acknowledge the valuable contributions made to the test program by numerous people. This test series was initially proposed by Dr. Novak Zuber of NRC. The test matrix and modifications were planned in co-operation with Prof. T.G. Theofanous of University of California, Santa Barbara. We are also grateful to Mr José Reyes of NRC for his co-operation and help.

Many people at IVO's Hydraulic Laboratory made significant contributions to the work, Mr. Pertti Mustonen being responsible for the practical part of the test program and acting as a co-author of the test report. Mr. Yrjö Tuokkola was responsible for the construction of the facility. Computer programming for data acquisition and reporting was done by Mr. Petri Vesikivi. Flow visualization drawings were done by Mr. Jouko Partonen.

Mr. Ismo Kokkonen has performed the REMIX calculations and comparison to the experimental data.

REFERENCES

- /1/ P. Mustonen, "Fluid and Thermal Mixing Tests of the Loviisa Pressure Vessel Downcomer", Imatran Voima Oy, Hydraulic Laboratory, Test Report RLB-322, March 1984.
- /2/ H. Tuomisto, Y. Hytönen and P. Mustonen, "Two-fifths Scale Thermal Mixing Tests in a Semiannular Downcomer and Cold Legs", Proceedings of the International ANS/ENS Topical Meeting on Thermal Reactor Safety, San Diego, California, 2-6 Feb 1986, Paper XVIII.5.
- /3/ H. Tuomisto and P. Mustonen, "Thermal Mixing Tests in a Semiannular Downcomer with Interacting Flows from Cold Legs", Imatran Voima Oy, Hydraulic Laboratory, Test

Report RLB-340, May 1986 (to be published as a
NUREG/IA-report)

- /4/ K. Iyer, H.P. Nourbakhsh and T.G. Theofanous, "REMIX: A
Computer Program for Temperature Transients Due to HPI
after Interruption of Natural Circulations", Purdue
University, USNRC Report NUREG/CR-3701, February 1986.
- /5/ H. Tuomisto, B. Mohsen, H. Kantee and J. Miettinen,
"Thermal Hydraulic Analyses of Selected Overcooling
Transients in the Probabilistic PTS Study of the Loviisa
Reactor Pressure Vessel", Transactions of the ENC'86
Conference, Geneva, Switzerland, June 1-6, 1986, Vol. 3,
pp. 475-483.

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SCALING OF THERMAL MIXING PHENOMENA FROM 1/5 TO FULL SCALE TEST FACILITIES

by

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Thermal mixing in relation to Pressurized Thermal Shock has been examined experimentally at various scales. In order of size these facilities include: CREARE (1/5), IVO (2/5), PURDUE (1/2) (presently at UCSB), CREARE (1/2), HDR and UPTF. The HDR experiments were run at prototypic pressure and temperature conditions; however, the cold leg diameter and downcomer gap in this reactor is only about one-half of those in full-scale commercial PWRs. The UPTF, on the other hand, is a full-scale replica of a PWR. Because of its pressure rating, however, it can operate only up to 200° C. The purpose of this paper is: (a) to show the first predictions (using the Regional Mixing Model-RMM) of the recently released UPTF data and (b) to demonstrate the use of RMM as a scaling tool over the whole range of scales.

1. INTRODUCTION

Thermal mixing phenomena associated with high pressure safety injection into the stagnated primary system loops of a PWR received considerable attention in the past 4-5 years in conjunction with the Pressurized Thermal Shock issue. The key developments, including both experimental and analytical efforts, have been recently summarized [1] and will not be repeated here. In this presentation we focus on the use of the Regional Mixing Model (RMM) [2] as a scaling tool, particularly in light of the full-scale UPTF data that have recently been made available.

The basic concept of the RMM is illustrated in Figure 1. The solution is obtained by partitioning the mean mass and energy into the hot and cold streams. For low Froude number injections (from the top of the cold leg) this is done consistent with plume mixing rates and counter-current flow limitations. For high Froude number injections the counter-current flow condition governs. Two computer programs, REMIX and NEWMIX, were developed for each of these two regimes, respectively [3]. Details of the mathematical model and comparisons with various experiments (CREARE-1/5 and Purdue-1/2 scale) may be found in References [4], [5], and [6]. Additional data and comparisons have been documented in References [1] (CREARE-1/2 and HDR) and [7] (Purdue-1/2). Reactor predictions were given in References [6], [8], [9], and [10].

2. A SAMPLE OF PAST COMPARISONS

A scaled representation of the various integral, thermal mixing facilities is shown in Figure 2. Experiments involved a wide range of injection geometries and Froude numbers. The

Purdue-1/2-scale tests were isothermal, utilizing salt-induced buoyancy. The CREARE-1/2, HDR and UPTF tests were thermal. The CREARE-1/5 and the IVO-2/5 tests combined thermal and solute effects. In the isothermal tests, mixing was deduced from concentration measurements, and since the mass of solute is a conserved quantity, this may be considered a direct measurement. For all non-isothermal tests mixing was deduced from temperature measurements. Clearly, this is an indirect approach and the utmost care is required to ensure proper accounting of all sources and sinks of thermal energy (i.e., heat stored in walls, losses to environment, heat exchange with adjacent fluid volumes, such as between the core region and the downcomer, etc.).

As mentioned above, detailed comparisons of RMM with data from all but the UPTF facilities have already been reported. The quality of these comparisons need only be represented here. A sample set is provided in Figures 3a to 6f for this purpose.

3. THE UPTF TESTS

For purposes of Thermal Mixing the UPTF is a full-scale replica of the cold leg and downcomer of a modern power reactor (PWR). The lower plenum and core regions are also represented, although with no particular simulation of their internal structures. A schematic of the facility is shown in Figure 7. The high pressure injection (accumulator line) to the side of cold leg is not used in U.S. Reactors, and it is expected to yield a typically low mixing. This was avoided by using the insert shown in Figure 8. Such geometry was previously tested in the UCSB-1/2-scale facility. The fountain-like effect was shown to create, even at low Froude number injections, an adequate degree of mixing.

Tests were carried out at injection flow rates of 5 to 70 kg/s which correspond to injection Froude numbers of 0.6 to 16. All runs were with single loop injection. Pre-test predictions were run with both REMIX and NEWMIX with the understanding that the effect of the strange fountain-like geometry would be bounded between the two. Inadvertently, the heat source through the core barrel was omitted in these calculations, which, thus, missed the mean system response somewhat. Taking this source into account improved the performance in the manner illustrated in the table below.

EFFECT OF HEAT THROUGH CORE BARREL

Q_{HPI} (kg/s)	ΔT (°C)*	ΔT (°C)**	Fr_{HPI}
5	2.3	-1.3	0.6 - 0.8
10	6.7	0.5	1.2 - 1.9
20	10.3	1.1	2.4 - 4.0
40	15.8	8.3	4.6 - 9.1
70	19.6***	6.4***	8.2 - 16.0

* Underprediction of mean system response temperature due to neglect of heating through the core barrel--at 2,000 s.

** Underprediction of mean system response temperature in post-test prediction--at 2,000 s.

*** Values at 1,500 s.

As it turned out, all of the cold stream (including downcomer plume) behavior is quite close to that predicted by NEWMIX. Sample comparisons from the 5 and 40 kg/s runs are shown in Figures 9a to 10f. Given the unusual and non-prototypic injection scheme the emphases in these comparisons should be in the consistent interpretations of the downcomer behavior.

The cold stream and upper downcomer data indicate an interesting two-period trend. This is unusual, i.e., not seen in other facilities, and it is not reflected in the numerical results--which are representative of the trends in the second period. Without the full set of data available yet we do not have an explanation of this effect.

4. CONCLUSIONS

The Regional Mixing Model approach, together with the computer programs REMIX and NEWMIX, provide the means for a unified interpretation of all available thermal mixing data--spanning the range from 1/5 to full-scale. Thus, these tools can be employed with confidence to predict reactor behavior.

ACKNOWLEDGEMENTS

Financial support by the Nuclear Regulatory Commission is gratefully acknowledged. We also wish to extend our appreciation to Dr. J. Reyes and Dr. N. Zuber of the U.S. Nuclear Regulatory Commission for their cooperation and encouragement.

REFERENCES

1. Theofanous, T.G. and K. Iyer, "Mixing Phenomena of Interest to SBLOCAs," Invited paper for the "Specialists Meeting on Small Break LOCA Analyses in LWRs," Pisa, Italy, June 23-27, 1985, 163-191, Nuclear Science and Engineering (in press).
2. Theofanous, T.G., H.P. Nourbakhsh, P. Gherson and K. Iyer, "Decay of Buoyancy Driven Stratified Layers with Application to PTS," Proceedings of the 11th Water Reactor Safety Research Information Meeting, National Bureau of Standards, Gaithersburg, MD, October 24-28, 1983, 185-186.
3. Iyer, K. and T.G. Theofanous, "REMIX - A Computer Program for Temperature Transients due to HPI of Stagnated Loop Flow," NUREG/CR-3701, January 1986.
4. Nourbakhsh, H.P. and T.G. Theofanous, "Decay of Buoyancy Driven Stratified Layers with Applications to PTS, Part I: The Regional Mixing Model," Nuclear Engineering & Design (in press).
5. Iyer, K. and T.G. Theofanous, "Flooding Limited Thermal Mixing - The Case of High-Pressure Injection," Proceedings of the Third International Topical Meeting on Reactor Thermal Hydraulics held at Newport, RI, October 15-18, 1985. (Accepted NSE).
6. Iyer, K. and T.G. Theofanous, "Decay of Buoyancy Driven Stratified Layers with Applications to PTS: Reactor Predictions," ANS proceedings of the 1985 National Heat Transfer Conference in Denver, Colorado, August 4-7, 1985, 358-371. (Accepted NSE).

BEST COPY AVAILABLE

7. Gherson, P., K. Iyer, H.P. Nourbakhsh and T.G. Theofanous, "Decay of Buoyance Driven Stratified Layers with Applications to PTS, Part II: Purdue's 1/2-Scale Experiments," Nuclear Engineering and Design (in press).
8. Theofanous, T.G., Contributing author in Selby, D.L., et al, "Pressurized Thermal Shock Evaluation of H.B. Robinson Unit 2 Nuclear Power Plant," NUREG/CR-4183 (ORNL/TM-9567), September 1985.
9. Theofanous, T.G., Contributing author in Selby, D.L., et al, "Pressurized Thermal Shock of the Calvert Cliffs Unit I Nuclear Power Plant," NUREG/CR-4022 (ORNL/TM-9408), September 1985.
10. Theofanous, T.G. and E. Shabana, "Demonstration of the Regional Mixing Model as a Scaling Tool," NUREG/CR report (in preparation).

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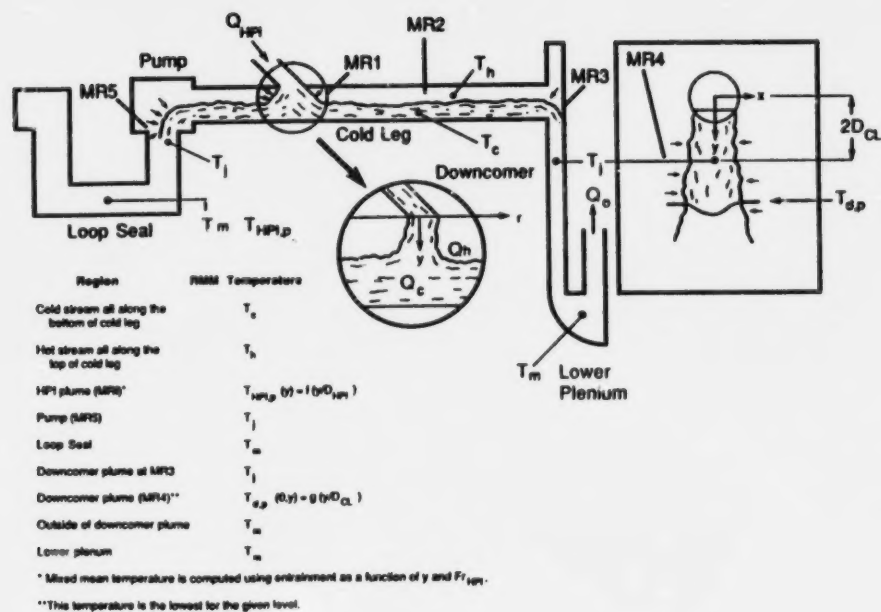


Fig. 1 Conceptual definition of flow regime and the Regional Mixing Model (RMM).

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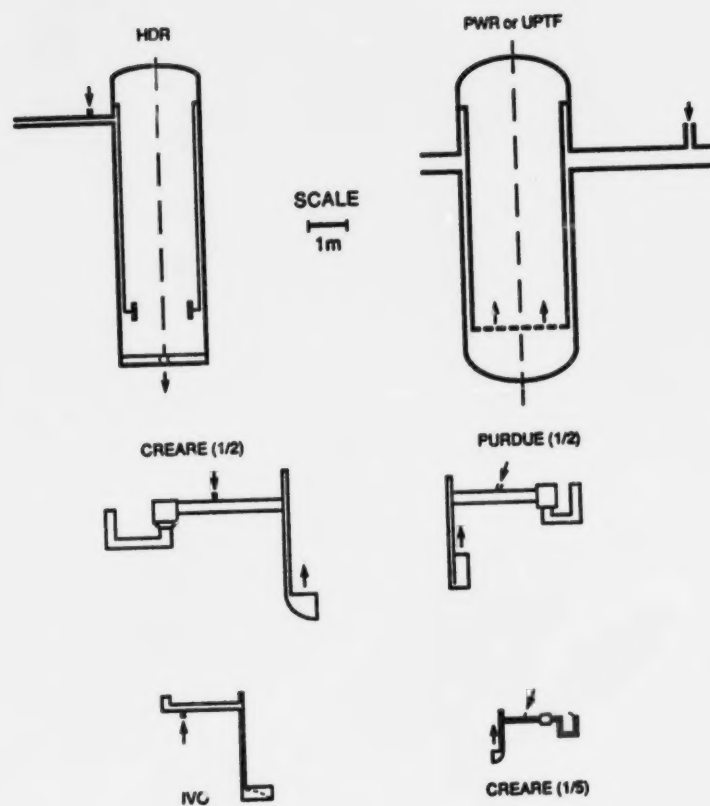


Fig. 2 Relative sizes of various integral test facilities in comparison to full scale commercial PWR.

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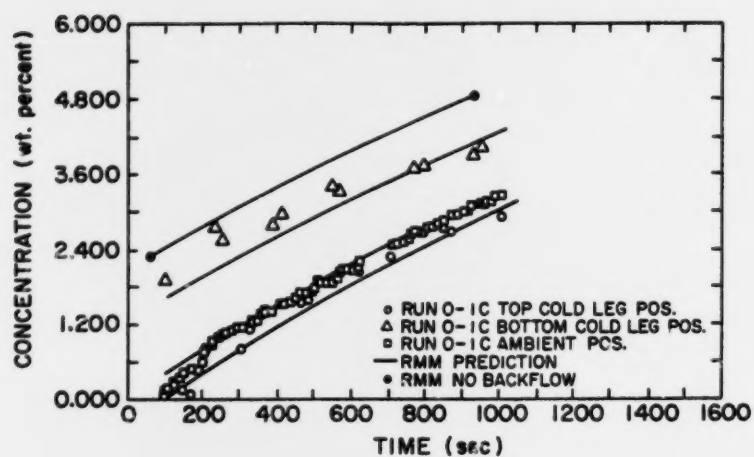


Fig. 3a Comparison of PURDUE 1/2 scale run 0-1C with REMIX predictions.

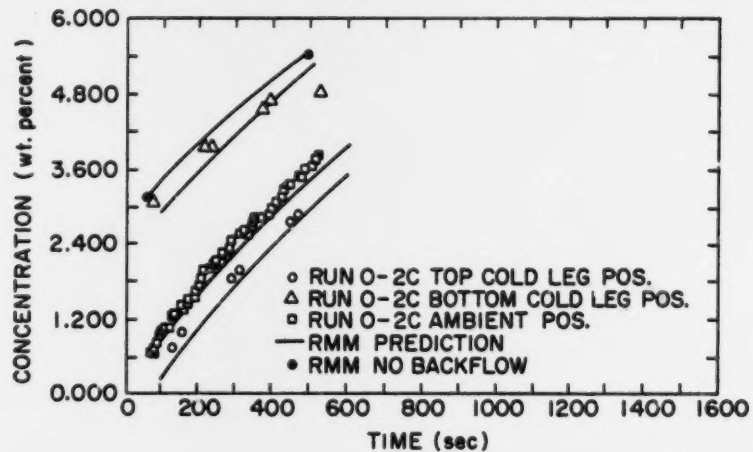


Fig. 3b Comparison of PURDUE 1/2 scale run 0-2C with REMIX predictions.

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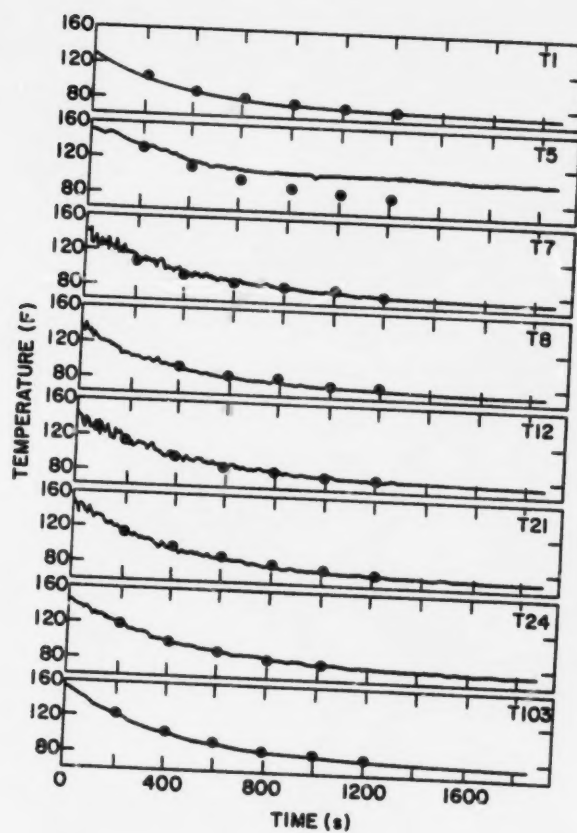


Fig. 4a Comparison of CREARE 1/5 scale run 100 with REMIX predictions (•).

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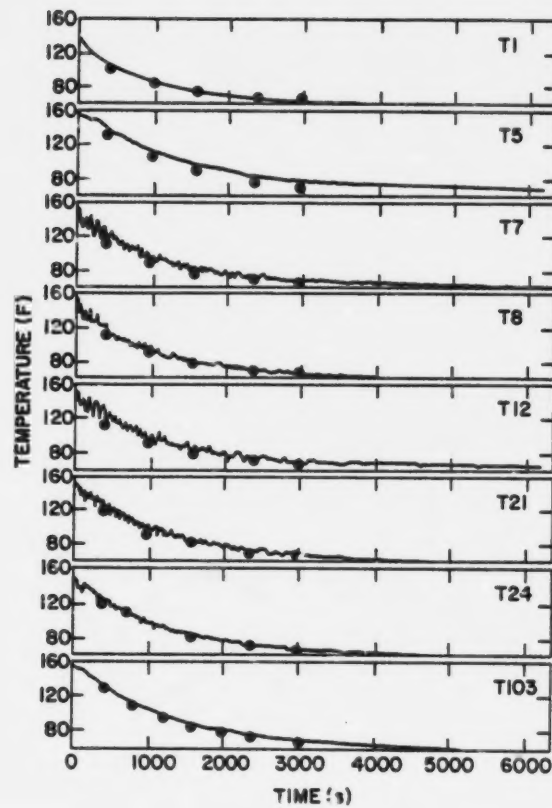


Fig. 4b Comparison of CREARE 1/5 scale run 106 with REMIX predictions (•).

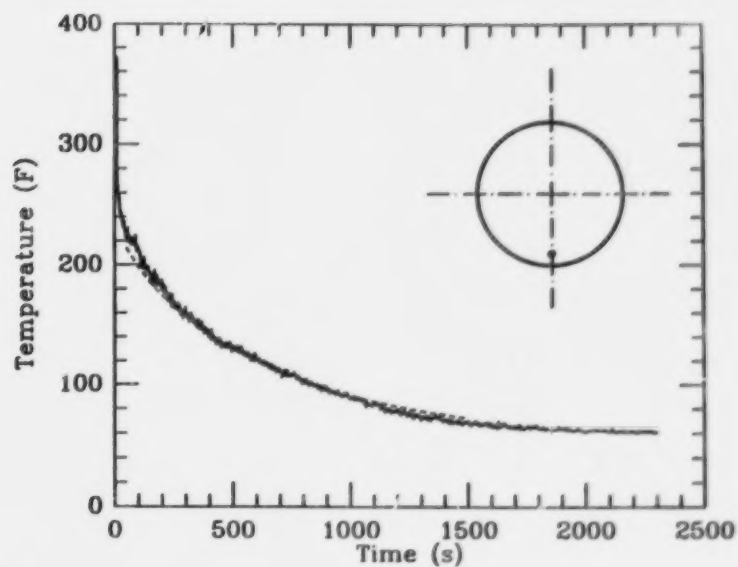


Fig. 5a Comparison of transient temperature response of cold stream at the exit of cold leg for CREARE run May 105 with REMIX present predictions T_c .

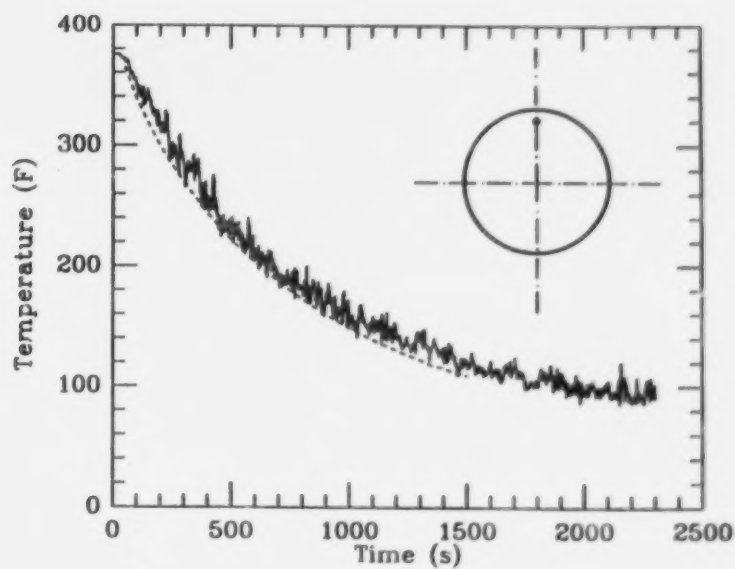


Fig. 5b Comparison of transient temperature response of hot stream at the exit of cold leg for CREARE run May 105 with REMIX present predictions T_h .

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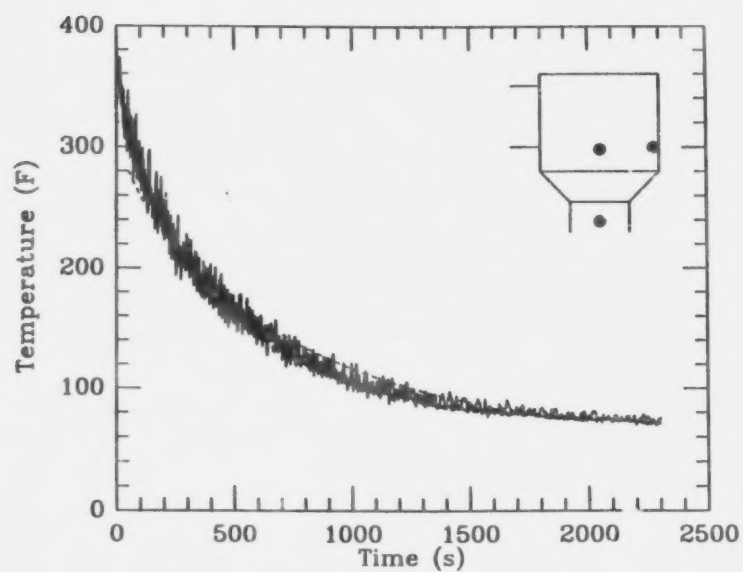


Fig. 5c Comparison of transient temperature response in pump for CREARE run May 105 with REMIX present predictions T_j .

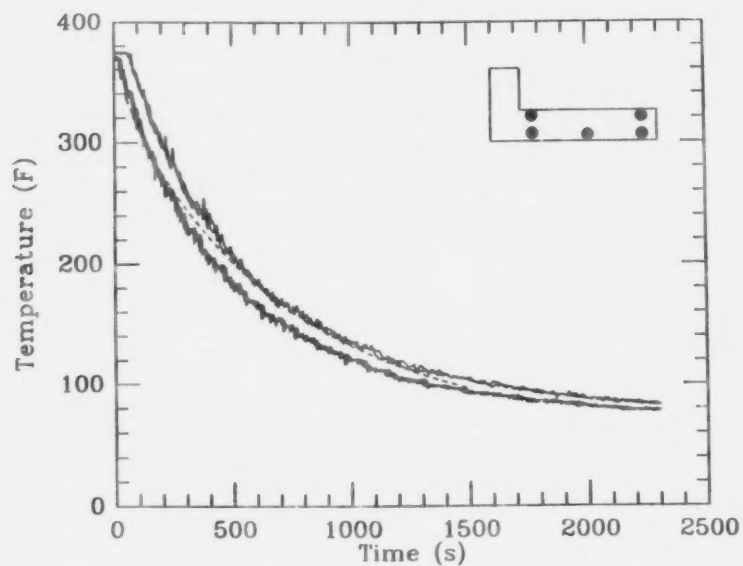


Fig. 5d Comparison of transient temperature response in loop seal for CREARE run May 105 with REMIX present predictions T_m .

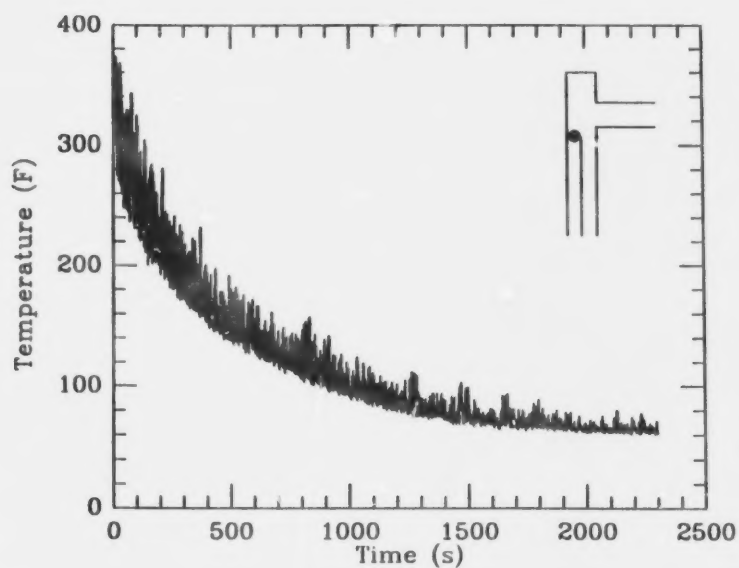


Fig. 5e Comparison of transient temperature response in downcomer at $1.1 D_{CL}$ below cold leg centerline for CREARE run May 105 with REMIX present predictions $T_{d,p}$ (---) core barrel side.

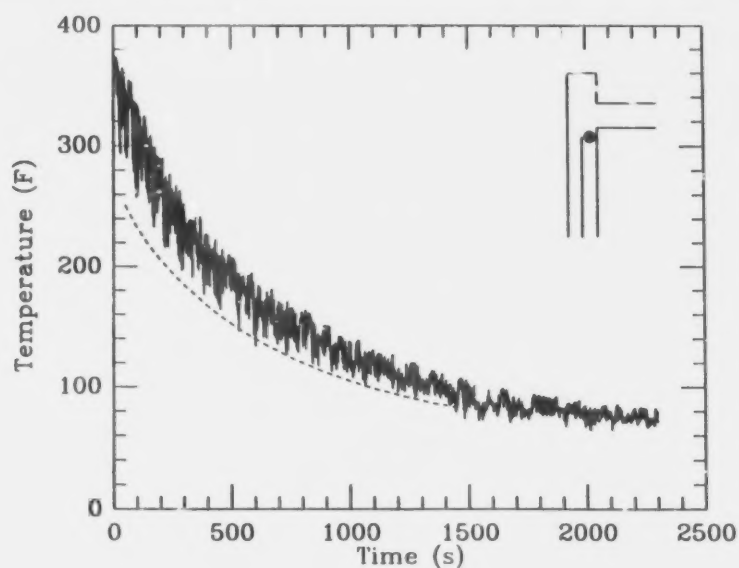


Fig. 5f Comparison of transient temperature response in downcomer at $1.1 D_{CL}$ below cold leg centerline for CREARE run May 105 with REMIX present predictions $T_{d,p}$ (---) vessel side.

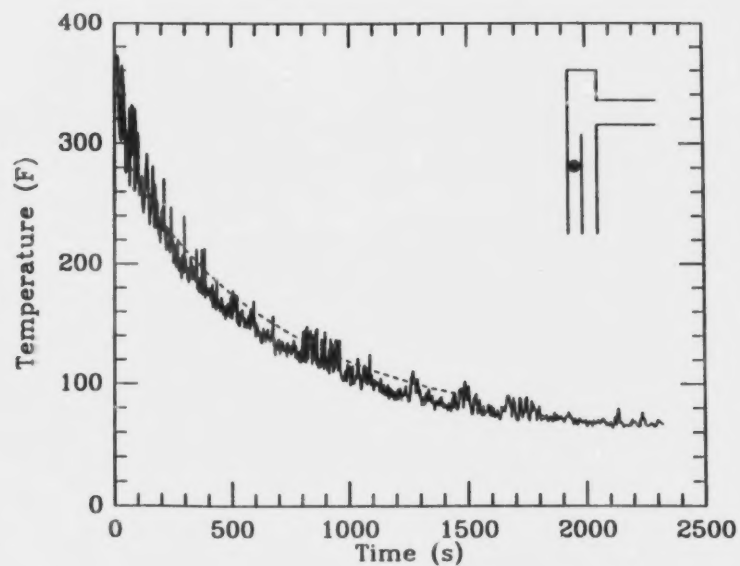


Fig. 5g Comparison of transient temperature response in downcomer at $2.1 D_{CL}$ below cold leg centerline for CREARE run May 105 with REMIX present predictions $T_{d,p}$ (---) core barrel side.

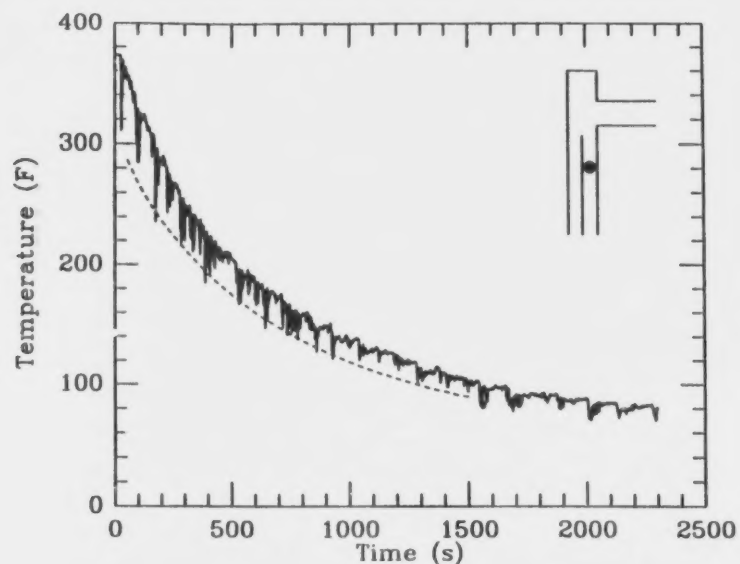


Fig. 5h Comparison of transient temperature response in downcomer at $2.1 D_{CL}$ below cold leg centerline for CREARE run May 105 with REMIX present predictions $T_{d,p}$ (---) vessel side.

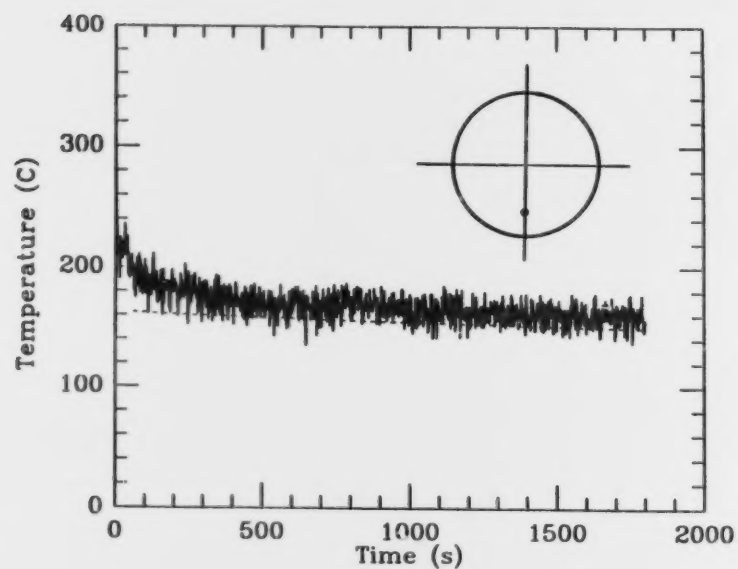


Fig. 6a Comparison of transient temperature response of cold stream at the exit of cold leg for HDR run T32.18 with REMIX present predictions T_c (---).

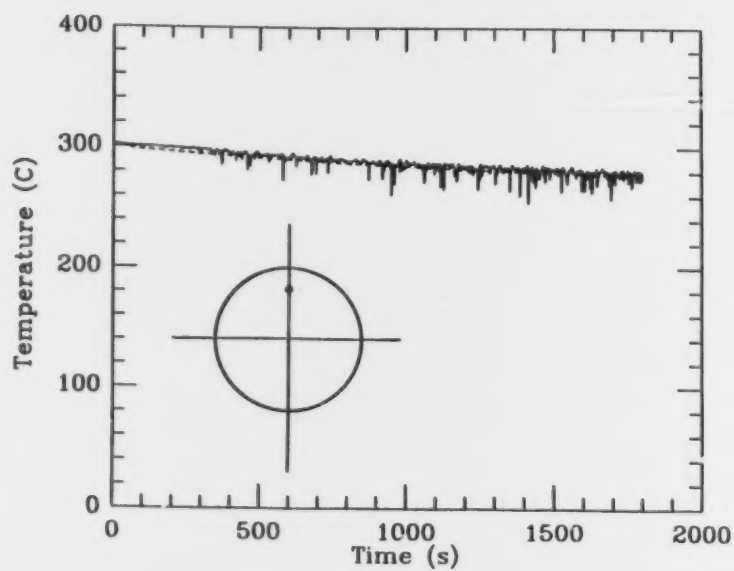


Fig. 6b Comparison of transient temperature response of hot stream at the exit of cold leg for HDR run T32.18 with REMIX present predictions T_h (---).

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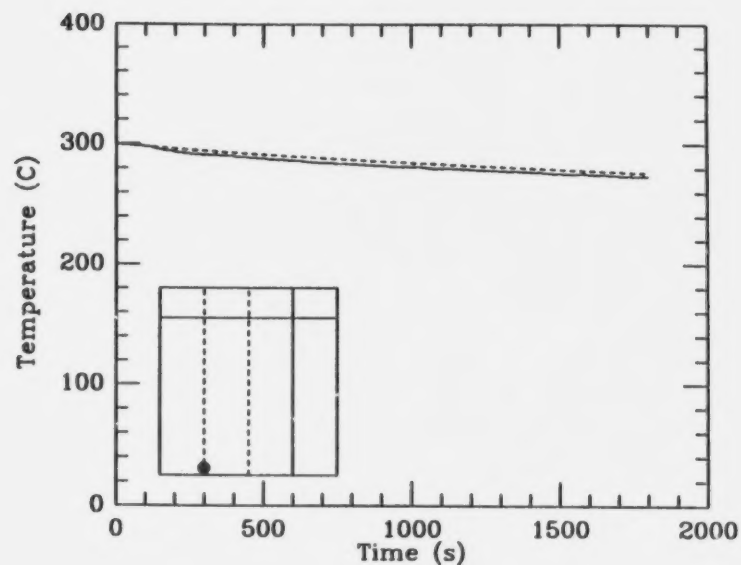


Fig. 6c Comparison of transient temperature response in downcomer faraway from the plume with REMIX present predictions T_m (---).

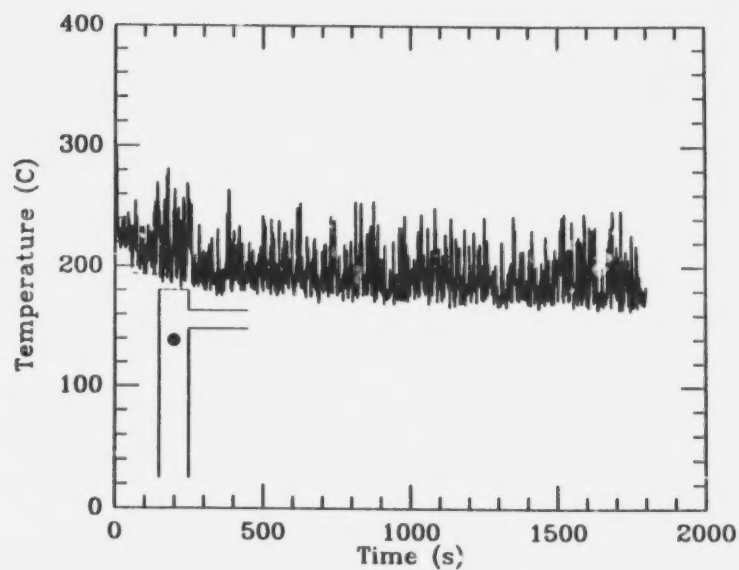


Fig. 6d Comparison of transient temperature response in downcomer at $1.0 D_{CL}$ below cold leg centerline for HDR run T32.18 with REMIX present predictions $T_{d,p}$ (---).

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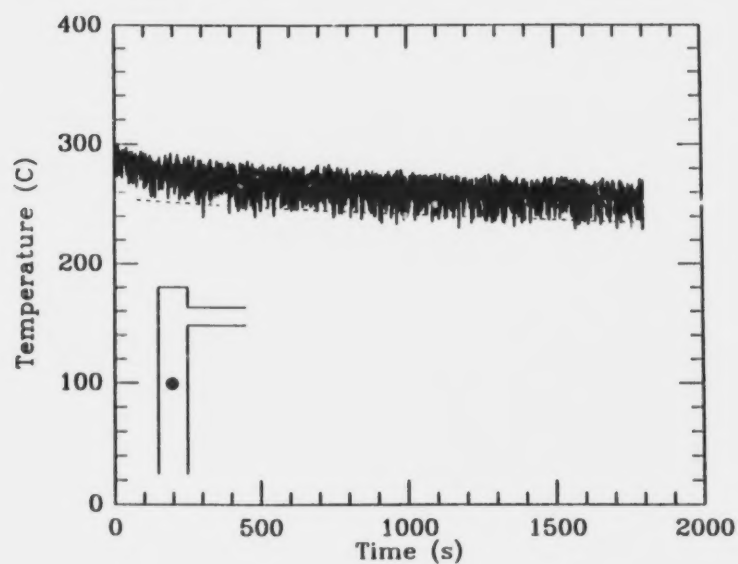


Fig. 6e Comparison of transient temperature response in downcomer at $5.2 D_{CL}$ below cold leg centerline for HDR run T32.18 with REMIX present predictions $T_{d,p}$ (---).

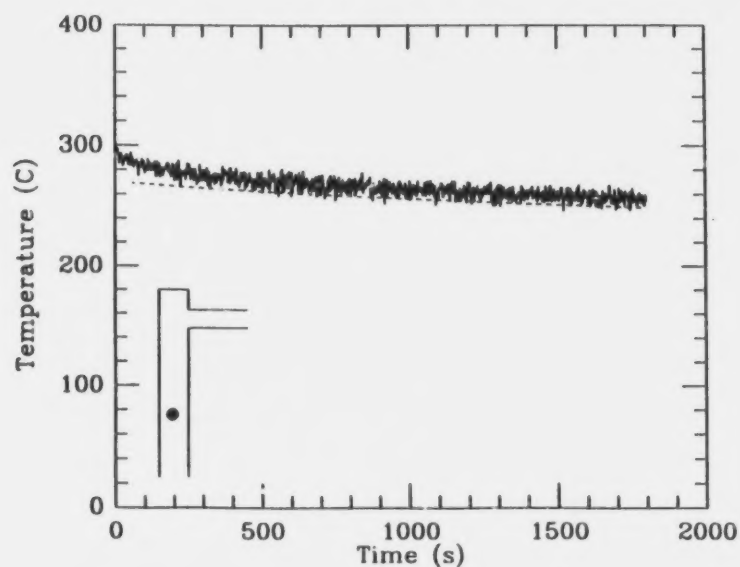


Fig. 6f Comparison of transient temperature response in downcomer at $8.6 D_{CL}$ below cold leg centerline for HDR run T32.18 with REMIX present predictions $T_{d,p}$ (---).

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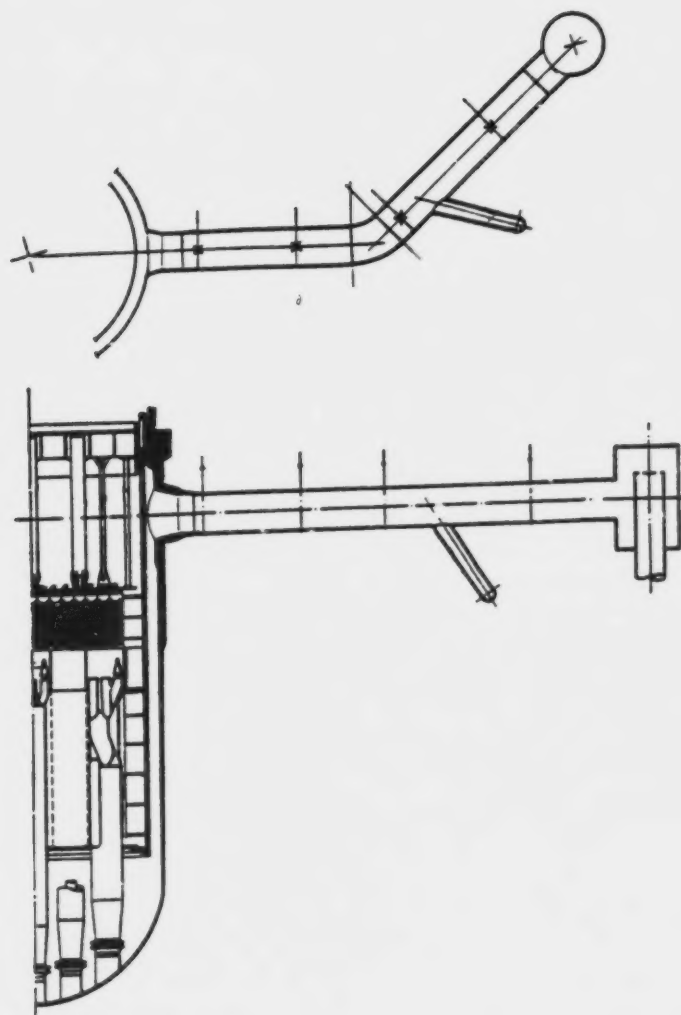


Fig. 7 UPTF test facility.

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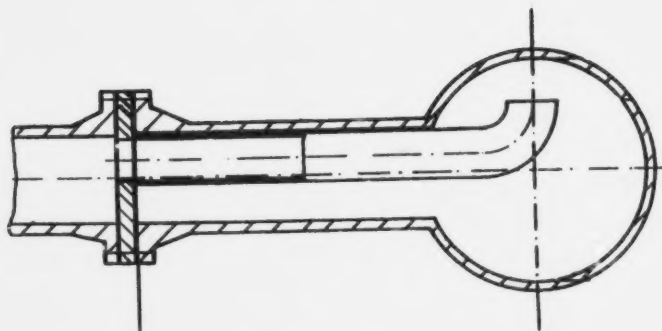


Fig. 8 High pressure injection pipe insert for UPTS test facility.

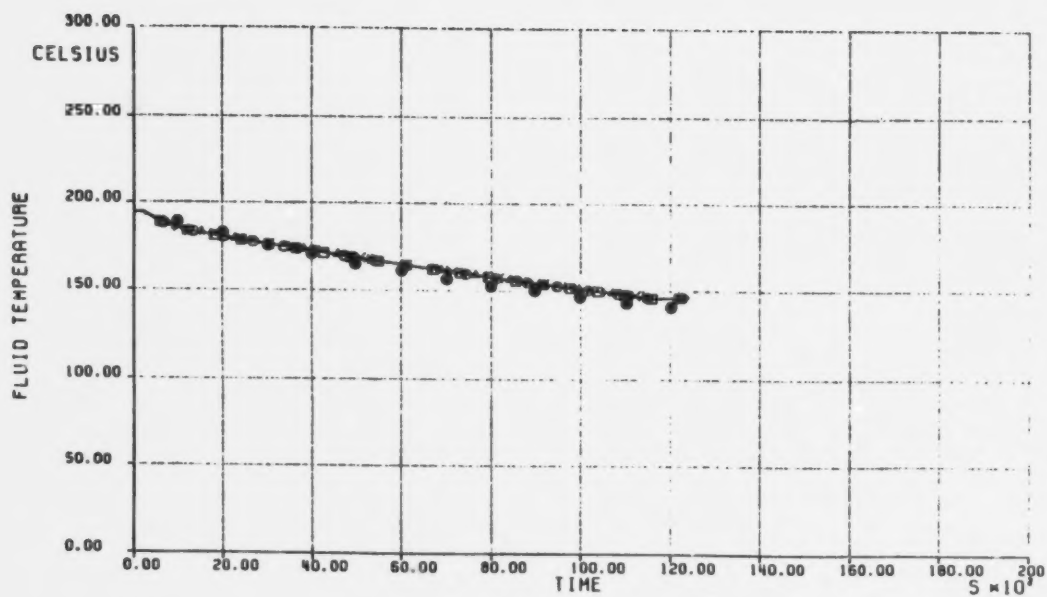


Fig. 9a Comparison of transient temperature response in lower plenum for UPTF run 023 with REMIX predictions T_m (•).

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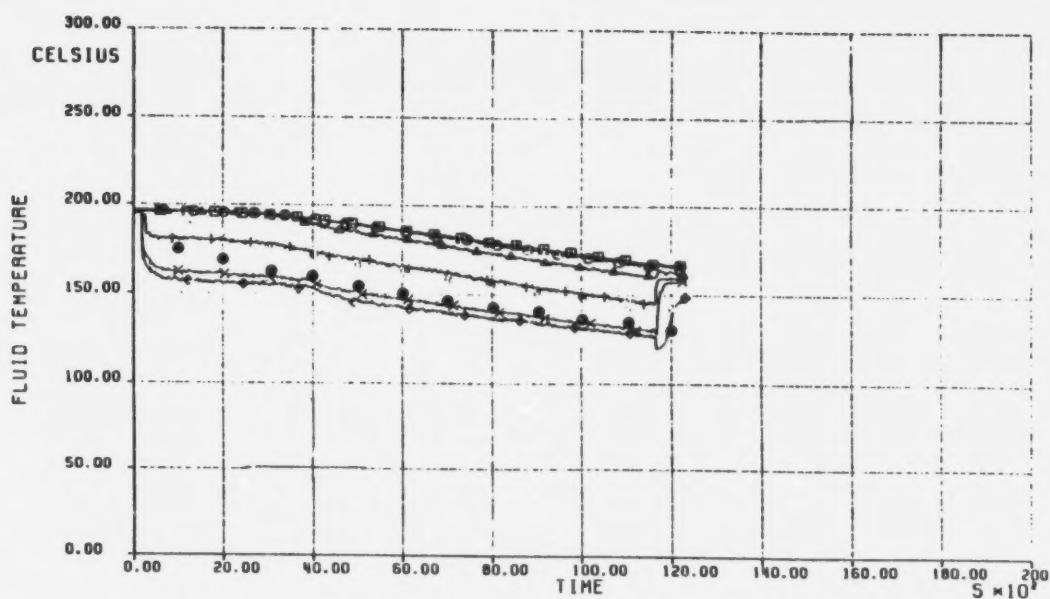


Fig. 9b Comparison of transient temperature response of cold stream at the exit of cold leg for UPTF run 023 with REMIX predictions T_c (•).

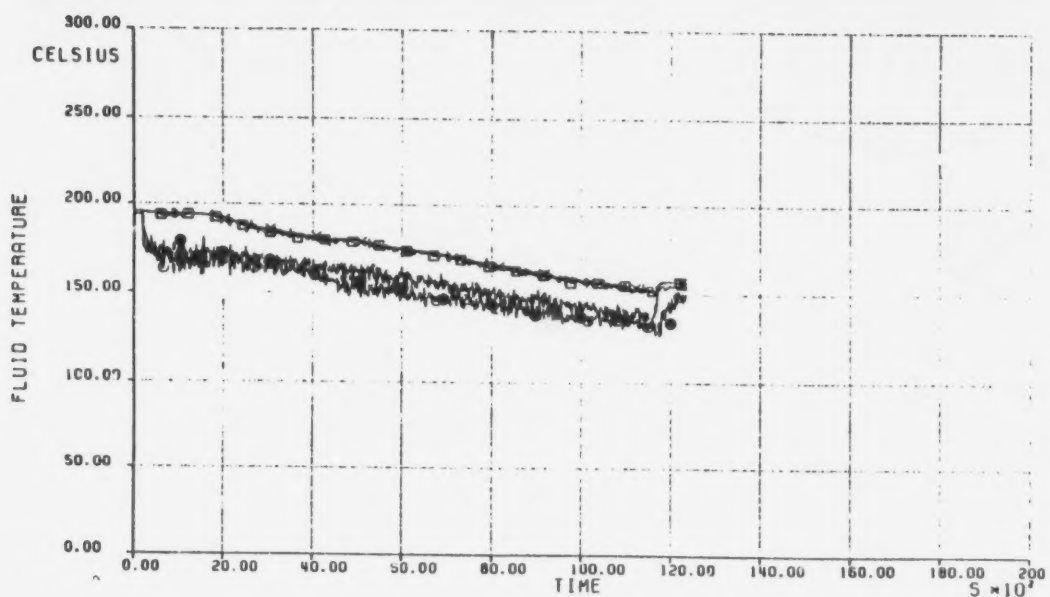


Fig. 9c Comparison of transient temperature response in downcomer at $1.0 D_{CL}$ below cold leg centerline for UPTF run 023 with REMIX predictions $T_{d,p}$ (•).

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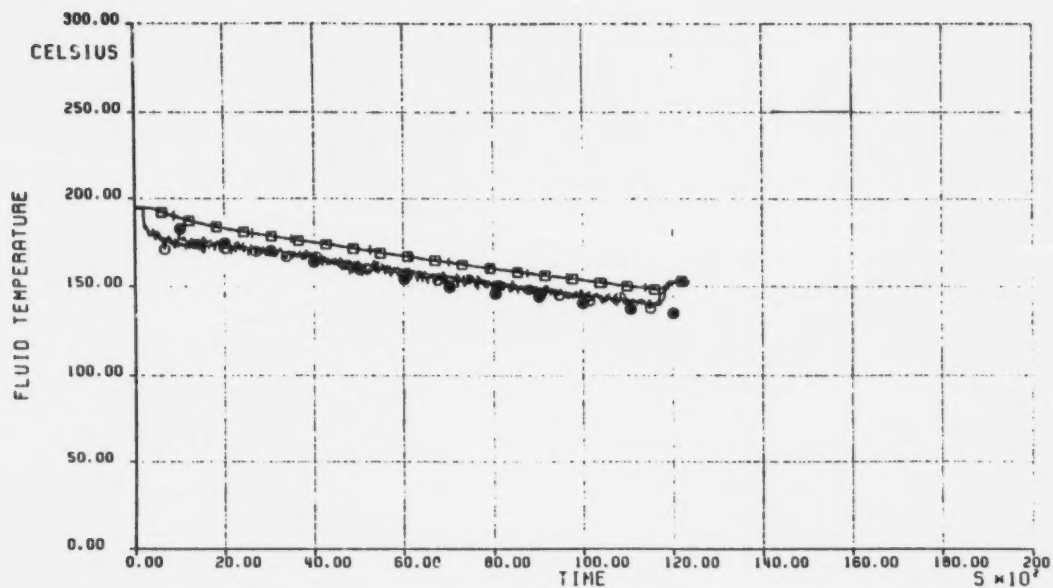


Fig. 9d Comparison of transient temperature response in downcomer at 2.0 D_{CL} below cold leg centerline for UPTF run 023 with REMIX predictions $T_{d,p}$ (•).

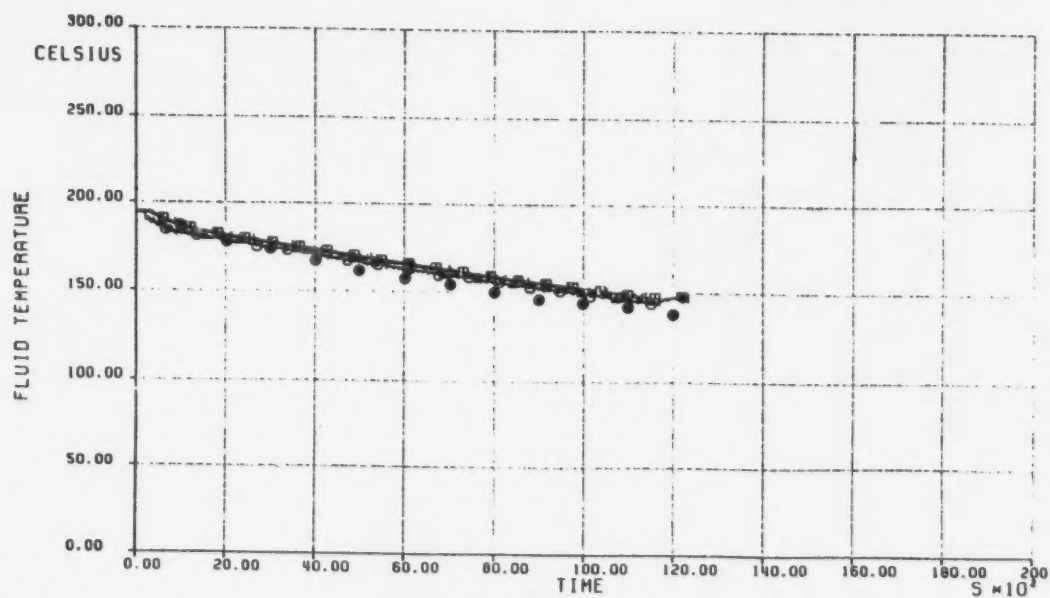


Fig. 9e Comparison of transient temperature response in downcomer at 5.0 D_{CL} below cold leg centerline for UPTF run 023 with REMIX predictions $T_{d,p}$ (•).

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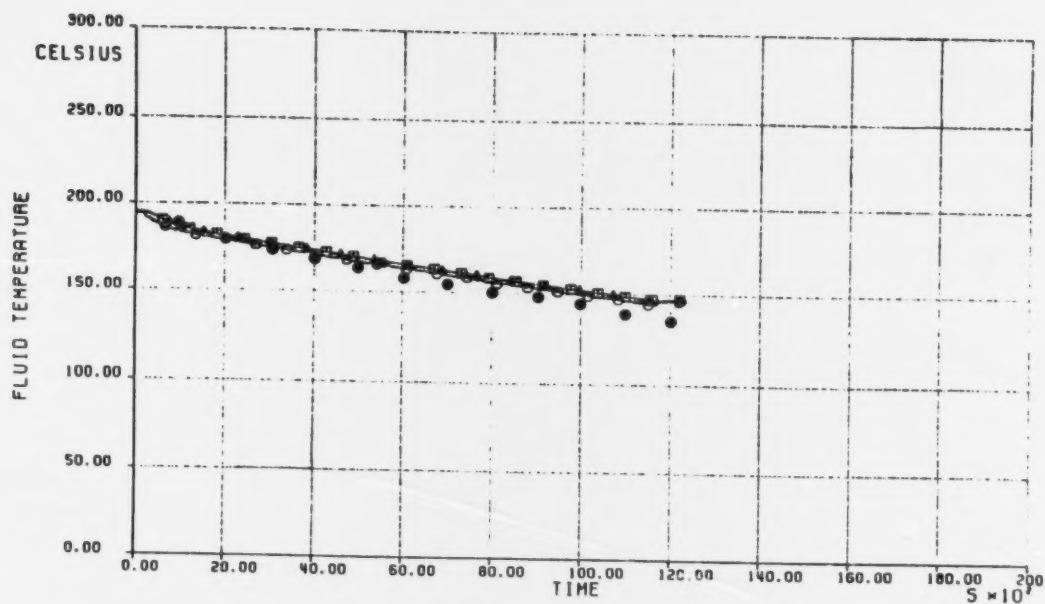


Fig. 9f Comparison of transient temperature response in downcomer at $7.0 D_{CL}$ below cold leg centerline for UPTF run 023 with REMIX predictions $T_{d,p}$ (*).

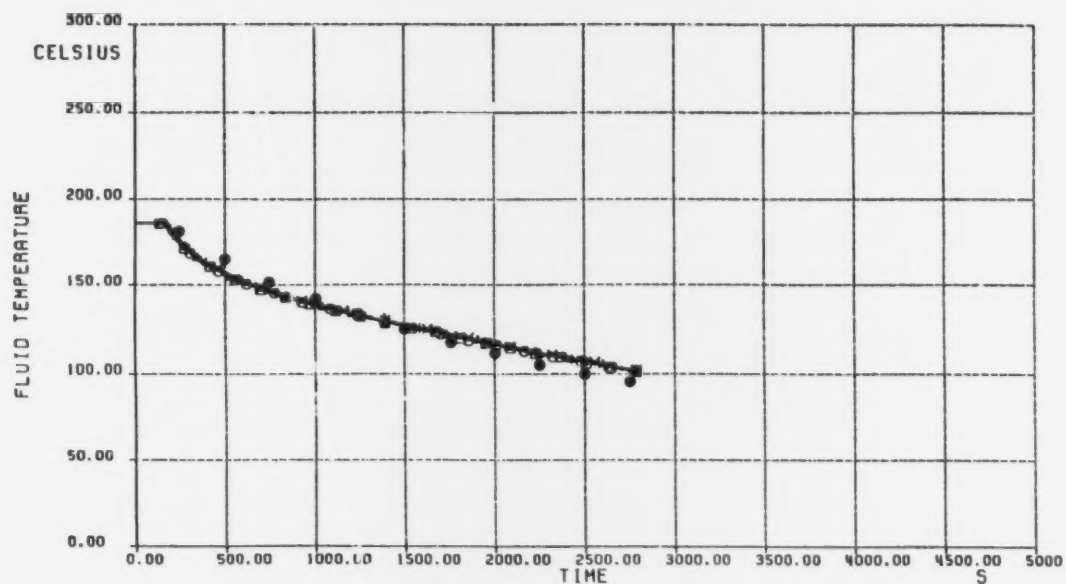


Fig. 10a Comparison of transient temperature response in lower plenum for UPTF run 021 with REMIX predictions T_m (*).

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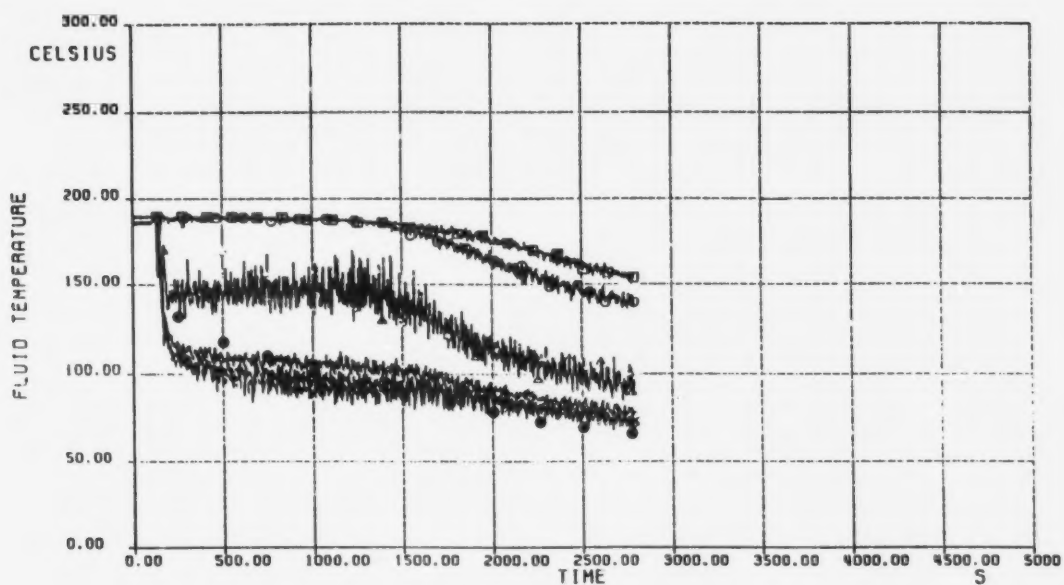


Fig. 10b Comparison of transient temperature response of cold stream at the exit of cold leg for UPTF run 021 with REMIX predictions T_c (•).

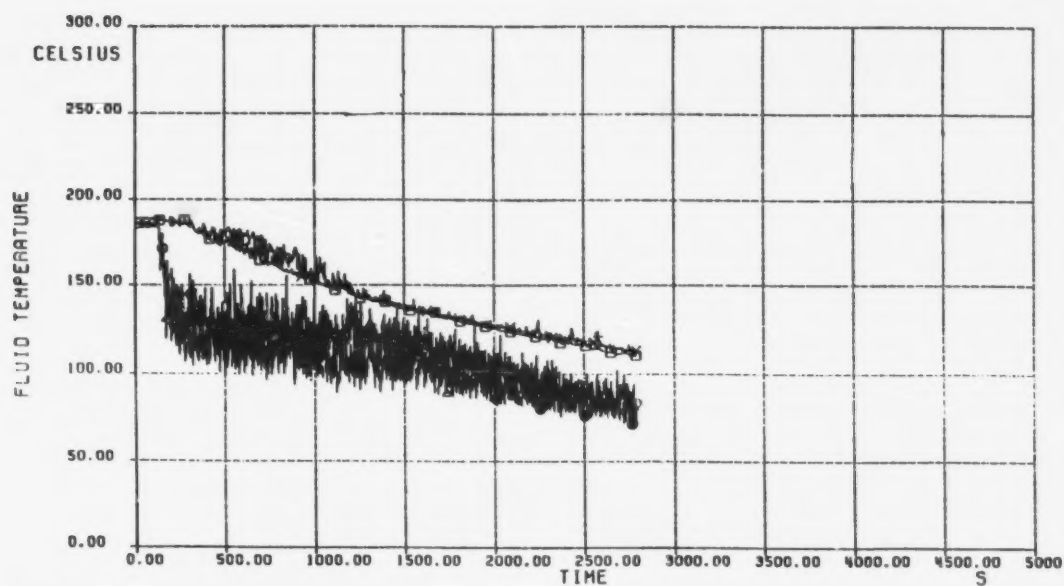


Fig. 10c Comparison of transient temperature response in downcomer at $1.0 D_{CL}$ below cold leg centerline for UPTF run 021 with REMIX predictions $T_{d,p}$ (•).

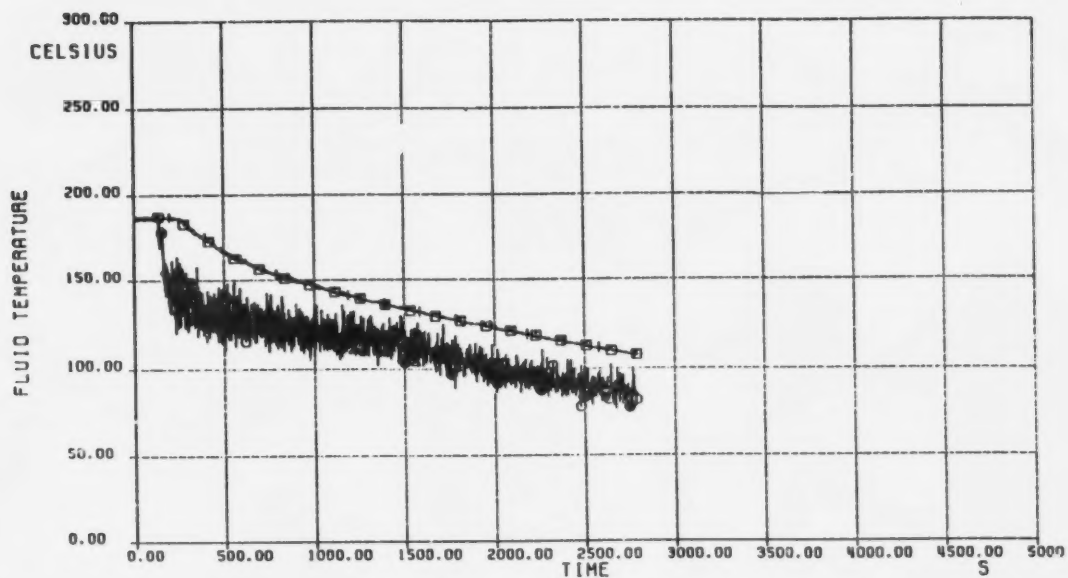


Fig. 10d Comparison of transient temperature response in downcomer at $2.0 D_{CL}$ below cold leg centerline for UPTF run 021 with REMIX predictions $T_{d,p}$ (•).

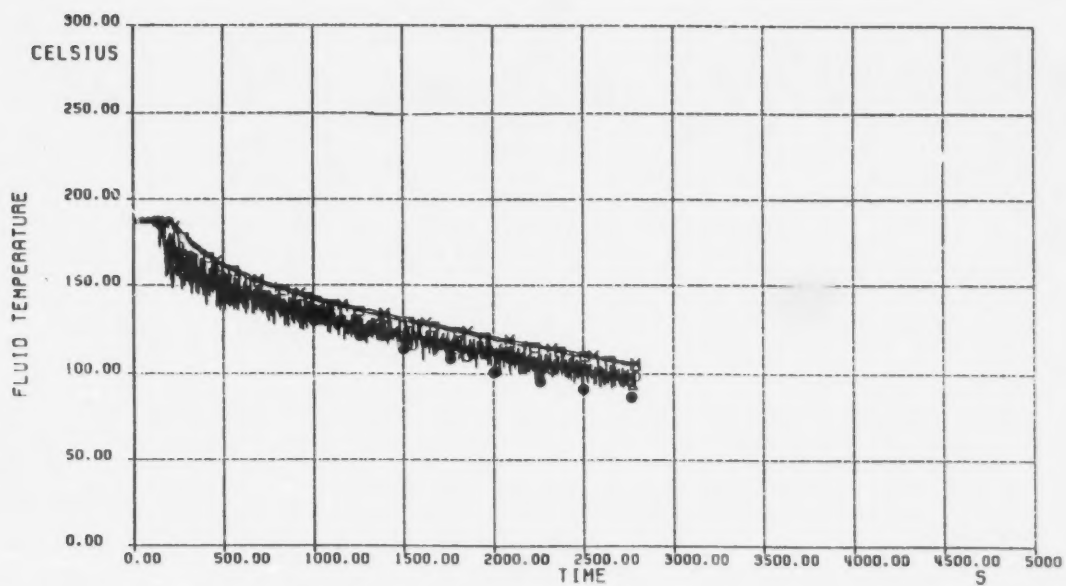


Fig. 10e Comparison of transient temperature response in downcomer at $5.0 D_{CL}$ below cold leg centerline for UPTF run 021 with REMIX predictions $T_{d,p}$ (•).

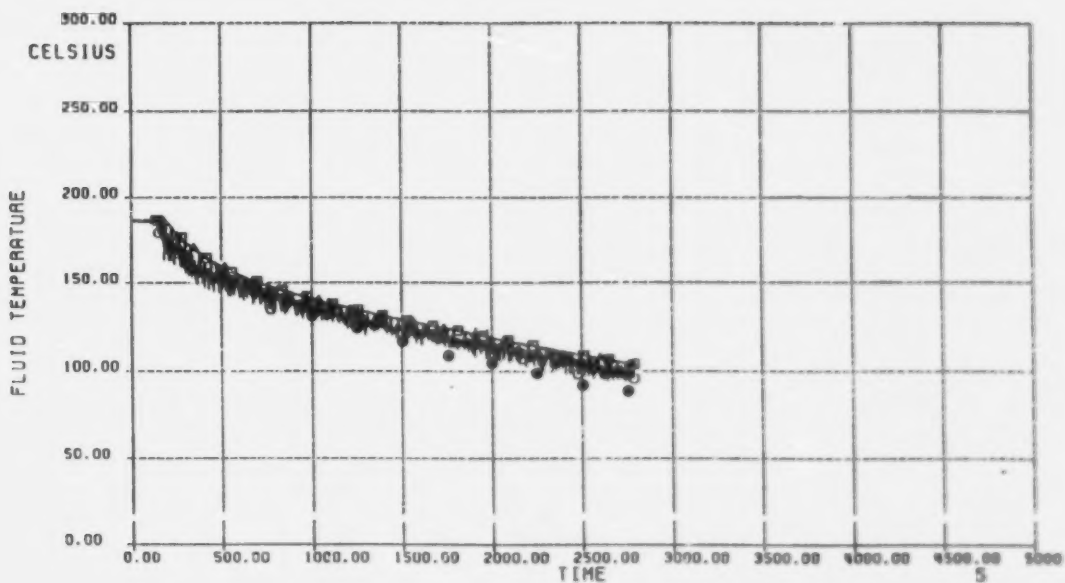


Fig. 10f Comparison of transient temperature response in downcomer at $7.0 D_{CL}$ below cold leg centerline for UPTF run 021 with REMIX predictions $T_{d,p}$ (•).

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Summary of CCTF test results

- Assessment of current safety evaluation analysis
on reflood behavior during a LOCA in a PWR
with cold-leg-injection-type ECCS -

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Hajime AKIMOTO, Tsutomu OKUBO, Tsuneyuki HOJO

Japan Atomic Energy Research Institute
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Abstract

Conservatism of current safety analysis was assessed by comparing the predicted result with cylindrical core test facility (CCTF) test result performed at Japan Atomic Energy Research Institute (JAERI). WREM code was selected for the assessment.

The overall conservatism of the WREM code on the peak clad temperature prediction was confirmed against CCTF EM test which simulated the typical initial and boundary conditions in the safety evaluation analysis. WREM code predicted the reasonable core boundary conditions and the conservatism of the code came mainly from core calculation. The conservatism of the WREM code against CCTF data could be attributed to the following three points.

- (i) No horizontal mixing assumption between subchannels at each elevation.
- (ii) No modeling on heat transfer enhancement caused by the radial core power profile.
- (iii) Conservative heat transfer correlations in the code

1. Introduction

A reflood test program⁽¹⁾ for a large-break loss-of-coolant accident (LOCA) of pressurized water reactor (PWR) has been conducted at Japan Atomic Energy Research Institute (JAERI), by using large scale test facilities, which are the Cylindrical Core Test Facility (CCTF)⁽²⁾ and the Slab Core Test Facility (SCTF)⁽³⁾. This program has been in a part of 2D/3D project which is performed by USNRC, BMTF and JAERI.

The CCTF is an experimental facility designed to model a

The work was performed under contract with the Atomic Energy Bureau of Science and Technology Agency of Japan.

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four-loop 1100 MWe class PWR with the flow area scaling ratio of 1/21.4 and to simulate the thermo-hydraulic behavior in the primary system during the refill and the reflood phases of a PWR-LOCA. The CCTF hsd pressure vessel (PV) with a cylindrical core of 1824 electrically-heated rods and four loops with passive and active components (for example, active steam generators, primary pumps). Since 1979, JAERI has performed 56 CCTF tests. They can be classified into 5 categories.

- (1) Cold-leg-injection simulation test under evaluation model (EM) condition.
- (2) Cold-leg-injection simulation tests for parametric effect study.
- (3) Cold-leg-injection tests to verify that the CCTF simulates a PWR properly.
- (4) Alternative ECC simulation tests, such as upper plenum injection, downcomer injection and combined injection (cold legs and hot legs).
- (5) Refill simulation tests to investigate the thermal hydraulics in the primary system during the end-of-blowdown to reflood initiation.

The test conduction of JAERI for CCTF has completed at March, 1985. The analytical works have been performed in the following fields.

- (1) Physical understanding on refill and reflood phenomena for PWRs with cold-leg-injection-type ECCS
- (2) Investigation on parameter effect on reflood phenomena
- (3) Evaluation on adequacy of similarity of CCTF to PWRs
- (4) Evaluation on conservatism of current safety analysis
- (5) Evaluation on predictability of best-estimate codes, i.e. TRAC and REFLA⁽⁴⁾
- (6) Physiscal understanding on reflood phenomena for PWRs with other injection types of ECCS (Downcomer injection, Upper plenum injection and Combined injection)

This presentation focuses on (4).

The evaluation is performed by comparing the predicted results with a safety evaluation code mainly against the CCTF experimental data and analyzing them. Through the evaluation, it is confirmed that the codes give the conservative prediction for reflood phase and also the differences in thermal-hydraulics between the prediction and the experimental results are identified.

The selected safety evaluation code for the evaluation is WREM code⁽⁵⁾ which has been released by USNRC and been often used for the current safety evaluation.

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2. Description of CCTF

The CCTF is designed to provide the capability to reasonably simulate the flow conditions in the primary system of a PWR during the refill and reflood phases of a large-break LOCA, and models a four-loop 1100 MWe class PWR with the flow area scaling ratio of 1/21.4.⁽³⁾ It has a scaled pressure vessel with a full height core and four loops with passive and active component simulators, eg. active steam generators, pump simulators and containment tank. The illustration of CCTF is shown in Fig.1.

The CCTF simulates a 200% cold-leg-break and can be operated at the pressure less than 0.6 MPa. The various types of ECCS (Cold leg injection, downcomer injection, upper plenum injection and combined injection) can be simulated in the CCTF.

The core has 1824 electrically heated rods arranged in cylindrical configuration, as shown in Fig.2(1). The core can be subdivided into three regions to establish a desired radial power profile, as indicated by High power zone, Medium power zone and Low power zone. An annulus downcomer with a gap of 61.5 mm surrounds the core. In the upper plenum, upper plenum structures scaled by 8/15 are installed. The arrangement of the guide tubes and the support columns are shown in Fig.2(2). Each heated rod in the core simulates a full-size 15 x 15 - array fuel rod. It consists of a spiral heater element, insulator, and inconel clad. The diameter and the heated length of the rod are 10.7 mm and 3.66 m, respectively, simulating undeformed 15 x 15 - bundle-type fuel.

3. Results and discussion

3.1 Comparison of data and requirement in guide line of safety evaluation

According to Japanese licensing, it is required to assume the followings for reflood phase.

1. Single failure of ECCS
2. 102% core overpower before onset of LOCA.
3. Steep core power profile
4. Sticking of primary pumps
5. Low pressure

Figure 3 shows the measured clad temperature at midplane of high-power rod in CCTF tests under simulating no failure and single failure of ECCS. The peak clad temperature under single failure of ECCS is about 10 K lower than that under no failure of ECCS, indicating that the single failure assumption of ECCS is not always conservative.

Fig.4 shows the effects of core power level, core power profile, loop K factor and pressure on the clad temperature. The higher core power level, the steeper core power profile, the higher loop K factor, and the lower pressure gives the higher peak clad temperature. Concerning the core cooling during reflood

phase of large-break-LOCA, it can be concluded that the above requirements are conservative against clad temperature in the scope of CCTF test results, except for the single failure assumption of ECCS.

The reason why the test under single failure of ECCS did not give the higher peak clad temperature than that under no failure of ECCS is explained as follows. The lower ECC injection rate simulating the single failure of ECCS caused the lower core flooding rate. However, it caused the reduction of steam condensation at ECC ports, resulting in the increase in steam outflow through PV side broken cold leg and then the increase in the differential pressure through PV side broken cold leg. This lead to the increase in pressure in pressure vessel and then the improvement of core cooling.

Although, according to CCTF data, the single failure assumption of ECCS is not always conservative, the differential pressure through PV side broken cold leg is usually modeled to be predicted very small in the current safety analyses, and the conservatism of prediction is preserved both in case of no failure and single failure of ECCS.

3.2 Overall conservatism of clad temperature prediction with WREM code

WREM code calculates the core boundary conditions (core flooding rate, core inlet fluid temperature and core inlet pressure) for system calculation as the first step, and then calculates the peak clad temperature for core calculation as the second step by using the core inlet boundary conditions calculated in system calculation.

Figure 5 shows the comparison of clad temperatures between the WREM prediction noted pre-calculation and the CCTF data. The referred CCTF test is the EM test, which major test conditions are equivalent to the initial and the boundary conditions predicted in the safety evaluation analysis. The clad temperatures in Fig. 5 indicate the temperature where the highest linear power density is supplied.

In the figure, another calculational result is shown as noted post-calculation. In the calculation, the boundary conditions at the core inlet were given based on the measured values in the CCTF test, so that it can be supposed that there is no error arising from the system calculation of the WREM prediction. The difference in clad temperature between the post-calculation prediction and the CCTF data is caused only by the core calculation in the WREM prediction.

The pre-calculation gives much higher clad temperature than data. It was confirmed that the WREM code conservatively predicts the core cooling.

The post-calculation gives lower clad temperature than the pre-calculation, but gives higher clad temperature than the data.

These indicate that the conservatism of the WREM code is lead mainly from the core calculation and that the system calculation of the WREM code itself introduces the small conservatism.

5.3 Result of system calculation with WREM code

In order to investigate the conservatism of system calculation with WREM code, calculated core boundary conditions are compared with data.

Core flooding rate

Figure 6 shows the experimental core flooding rate and the calculated one. The experimental one was obtained by mass balance calculation using measured data. It is not sufficiently accurate before 30 s due to smoothing of data, so that it is difficult to compare quantitatively between the experimental and the calculated during the period. However, the calculated during the period is higher than the experimental on the time average. Good prediction is obtained after 40 s.

Since core flooding rate is governed by mass and momentum balance in entire system, the above result indicates that the system model in WREM code is conservative or well-balanced for evaluating the flooding rate which is key-parameter to predict the core heat transfer.

Pressure at core inlet

Figure 7(a) shows the measured and the predicted pressures at core inlet. The predicted is lower than the measured.

The pressure at core inlet P_C can be written as follows.

$$P_C = \Delta P_D + \Delta P_{BCL} + P_{CT}$$

P_C , P_{CT} , ΔP_D and ΔP_{BCL} are absolute pressure in core and containment tank and differential pressure across downcomer and broken cold leg nozzle, respectively.

The lower predicted pressure at core inlet was introduced by the lower pressure drop at the broken cold leg nozzle. Since the pressure is one of most sensitive parameter for core heat transfer⁽⁴⁾ and the lower pressure causes poorer core cooling⁽⁴⁾, the predicted is conservative for clad temperature prediction.

Fluid temperature at core inlet

Figure 7(b) shows the measured and the predicted fluid temperature at core inlet. The prediction is higher than the measured. This is introduced from the input of the calculation, where the ECC water temperature was set to be nearly saturated. However, the core inlet fluid temperature required in the core calculation of WREM code is usually treated as an input which is independent of the predicted core inlet fluid temperature in the system calculation. The value of the subcooling, 64 K, was

determined in the present WREM calculation based on the well-mixed water temperature in the lower plenum at the reflood initiation. The fluid temperature is much lower than the measured.

Since the higher temperature generally causes poorer core cooling, this input is not always conservative.

3.4 Comparison between the assumptions in WREM code and the data

In order to investigate the validity of assumptions in WREM code, the assumptions used have been compared with the data. The detailed result is described in References (6) and (7). The summary is tabulated in Tables 1 and 2. The CCTF data supported the following assumptions.

- (1) One dimensional thermal-hydraulics in primary system except for core
- (2) Thermal-equilibrium in primary system except for core
- (3) Average power rod for system calculation
- (4) Modeling of intact loops
(Representative one loop for three intact loops)
- (5) Modeling of steam generator heat transfer
(Convective heat transfer in secondary side)
- (6) Treatment of ECC water
(Saturated ECC injected into downcomer)

However, the following assumptions were qualitatively different from data.

- (1) Modeling of upper plenum leads to no water accumulation in upper plenum.
- (2) Modeling of differential pressure through PV side broken cold leg leads to very small differential pressure.
- (3) Modeling of downcomer leads to filling-up of downcomer and no distribution of fluid temperature.
- (4) Modeling of lower plenum leads to no distribution of fluid temperature.
- (5) Modeling of core leads to no heat transfer enhancement due to radial power profile and no fluid communication among subchannels.

3.5 Contribution of qualitative differences between the assumptions and data on core cooling

Qualitative differences between the assumptions and data are identified in 3.4. In order to confirm the conservatism of WREM code, it is necessary to assess the effects of above differences on peak clad temperature.

As already shown, the overall conservatism of WREM code comes from core calculation rather than system calculation. Therefore, item (5) in 3.4 is most important in comparison with item (1) - (4).

Water accumulation in upper plenum

The effect of water accumulation in upper plenum on peak clad temperature was assessed by using REFLA code with and without upper plenum water accumulation model. The predicted peak clad temperature with upper plenum water accumulation is about 70 K lower than that with no upper plenum water accumulation. It is confirmed that the treatment in WREM code is conservative by using REFLA code prediction.

Differential pressure through PV side broken cold leg

Differential pressure through PV side broken cold leg increases the pressure in the pressure vessel and then core flooding mass flow rate. Accordingly, the effect of differential pressure through PV side broken cold leg on peak clad temperature was assessed with REFLA code by giving a lower pressure and a lower core flooding mass flow rate than data.

The predicted peak clad temperature with simulating actual situation (large differential pressure through PV side broken cold leg) is about 30 K lower than that without simulating it. It is confirmed that the treatment in WREM code is conservative.

No-filling-up of downcomer

According to CCTF data, measured static head of water in downcomer was less than the estimated one in the situation of complete filling-up of downcomer. This was considered to be due to voiding and swelling in downcomer. Since the static head of water in downcomer is a driving force to supply water into core, lower static head causes the lower core flooding mass flow rate and the poorer core cooling. Accordingly, the treatment causing no reduction of the static head is unconservative.

However, according to CCTF tests with hot downcomer wall and cold downcomer wall, the increase of peak clad temperature due to reduction of the static head was much smaller in comparison with the margin of WREM code. Since then, overall conservatism of WREM code is considered to be preserved.

Fluid distribution in downcomer and lower plenum

According to CCTF data, water in bottom half of lower plenum was stagnated and saturated water from downcomer flowed to core inlet through upper layer of lower plenum. This caused that fluid temperature at core inlet became saturation in rather short time.

However according to CCTF and FLECHT data, fluid temperature at core inlet was not sensitive to peak clad temperature, although it was sensitive to quench time. Since then, overall conservatism of WREM code is considered to be preserved.

Fluid communication among subchannels

In WREM code, the clad temperature is predicted by combining the core boundary conditions of the system calculation and the highest power rod. This is corresponding to the assumption that

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there is no horizontal communication of fluid between subchannels at each elevation.

On one hand, Fig. 8 shows that the differential pressure in core (or static of water accumulation) can be predicted with infinite fluid communication assumption. This is a different point assumed in WREM code. The assumption of no horizontal communication may affect the core cooling.

The estimation on the effect of the above assumption was made by using REFLA code. Figure 9 shows the comparison of the predicted clad temperatures between infinite horizontal fluid communication model and no horizontal fluid communication model. Generally, the predicted clad temperature with infinite fluid communication is lower than that with no fluid communication, although the discrepancy of the peak clad temperatures between two calculations is only 10 K in this case.

It is inferred by REFLA code that no fluid communication assumption in the WREM code is conservative.

Heat transfer coefficient enhancement due to radial core power profile

According to CCTF test with radial core power profile (radial peaking factor = 1.364), the heat transfer coefficient at the elevation of 1.83 m in the central (High power) region is about 60% higher than that in the peripheral (Low power) region at 100 s, as shown in Fig. 10. The heat transfer coefficient in the central region is also higher at the elevation of 2.44 m than that in the peripheral region. These results show that heat transfer in the central (High power) region is enhanced due to the effect of the core radial power profile with a radial peaking factor of 1.364.

The effect of the radial core power profile was assessed by comparing the clad temperature predicted by two calculations, in which two different heat transfer coefficients combining the identical power inputs with each other calculation were used, that is, h_{steep} (measured heat transfer coefficient at the elevation of 1.83 m in the central region from the steep radial power test) and h_{flat} (measured heat transfer coefficient at the elevation of 1.83 m in the central region from the flat radial power test). The calculation with h_{steep} represents the calculation taking into account of the heat transfer enhancement caused by the radial core power profile. The calculation with h_{flat} represents the calculation taking into account of no radial core power profile effect, corresponding to the treatment of the WREM code.

The calculated clad temperature is shown in Fig. 11. The clad temperature with h_{steep} is lower than that with h_{flat} , although the discrepancy of the peak clad temperatures between two calculations is only 14 K in this case.

It is inferred from the above discussion that the neglect of the heat transfer enhancement caused by the ununiform radial core power profile in WREM code is conservative.

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Conservative heat transfer correlation in WREM code

FLECHT correlation is used in WREM code for heat transfer coefficient. Figure 12 shows results of three calculations. One of them, noted as WREM, is obtained by using FLECHT correlation. Others, noted as REFLA, are obtained with REFLA code, in which Murao-Sugimoto correlation is mainly used. Murao-Sugimoto correlation has been demonstrated for data with small scale facility during reflood phase.

The predicted by REFLA with no horizontal fluid communication is much lower than that with WREM. This difference is caused by the different heat transfer coefficient. It is inferred that the WREM code uses more conservative heat transfer correlation against CCTF data than REFLA code.

Comparing with data, it is known that REFLA itself gives conservative prediction. Since then, it can be said that one of reasons for overall conservatism of WREM code is conservative heat transfer correlation.

According to the present calculations shown in Fig. 12, it is inferred that the usage of conservative heat transfer correlation is most dominant to conservatism of WREM code.

4. Conclusions

- (1) The overall conservatism of the WREM code on the peak clad temperature prediction is confirmed against CCTF EM test which simulates the typical initial and boundary conditions in the safety evaluation analysis.
- (2) WREM code predicts the reasonable core boundary conditions and the conservatism of the code comes mainly from core calculation.
- (3) The conservatism of the WREM code against CCTF data can be attributed to the following three points.
 - (i) No horizontal mixing assumption between subchannels at each elevation.
 - (ii) No modeling on heat transfer enhancement caused by the radial core power profile.
 - (iii) Conservative heat transfer correlations in the code.

References

- (1) Hirano, K. and Murao, Y.: J. Nucl. Sci. Technol, 22(10), 681 (1980)
- (2) Murao, Y., et al. : J. Nucl. Sci. Technol, 19(9), 705 (1982)
- (3) Adachi, H., et al.: NUREG/CP-0048, 2, 277 (1984)
- (4) Murao, Y., et al. : JAERI-M 84-243 (1984)
- (5) WREM : NUREG-75/056 (1975)
- (6) Murao, Y., et al. : Proc. 2nd Int. Top. Mtg. on Nucl. Power Plant Thermal Hydr. and Oper. (1986)
- (7) Iguchi, T., et al.: ibid (1986).

Table 1 Typical assumptions used in execution
of WREM code (System Calculation)

Typical assumptions used	CCTF data for prediction
One dimensional hydraulics in primary system	One-dimensional water accumulation in downcomer and core
Thermal equilibrium except core	Thermal equilibrium except core
Average power rod for system calculation	Identical system response between cores of steep and flat power profile
Modeling of intact loops (One loop representing for three intact loops)	Identical differential pressures among each intact loop No parallel oscillation
Modeling of steam generator heat transfer (Convective heat transfer in secondary side)	Measured fluid temperature at outlet agrees with prediction
Treatment of ECC water (Saturated ECC injected into downcomer)	ECC water condenses steam stably at cold leg, and then cold leg injection can be regarded as downcomer injection

Typical assumptions	CCTF data against prediction
Upper plenum modeling (1 node, homogeneous)	Water accumulation in stead of no water accumulation
Broken cold leg differential pressure modeling (Separation of phases)	Significant differential pressure in stead of little one
Downcomer modeling (1 node, phase separation)	Reduced water level in stead of filling-up Distributed fluid temperature
Lower plenum modeling (1 node)	Distributed fluid temperature

Table 2 Typical assumptions used in execution
of WREM code (Core calculation)

Typical assumptions	CCTF data against prediction
No horizontal fluid mixing among subchannels	Core differential pressure is predicted well by full mixing assumption
No modeling on heat transfer enhancement caused by radial core power profile	Heat transfer enhancement caused by radial core power profile
Usage of FLECHT correlation with coefficient for EM and steam cooling for low core flooding case	Measured clad temperature is much lower than prediction

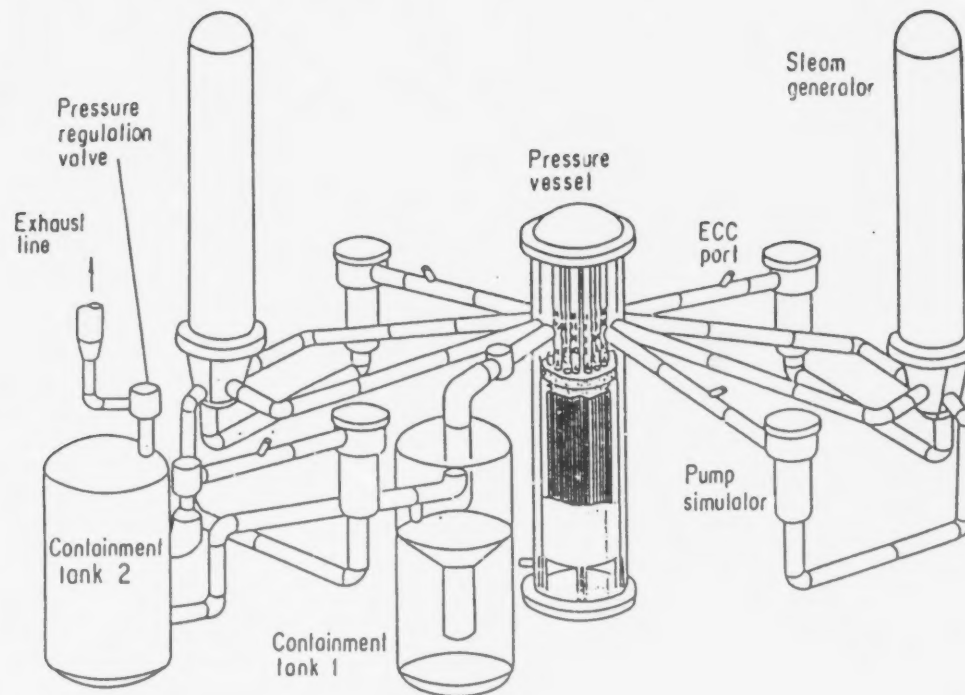


Fig. 1 Cylindrical core test facility

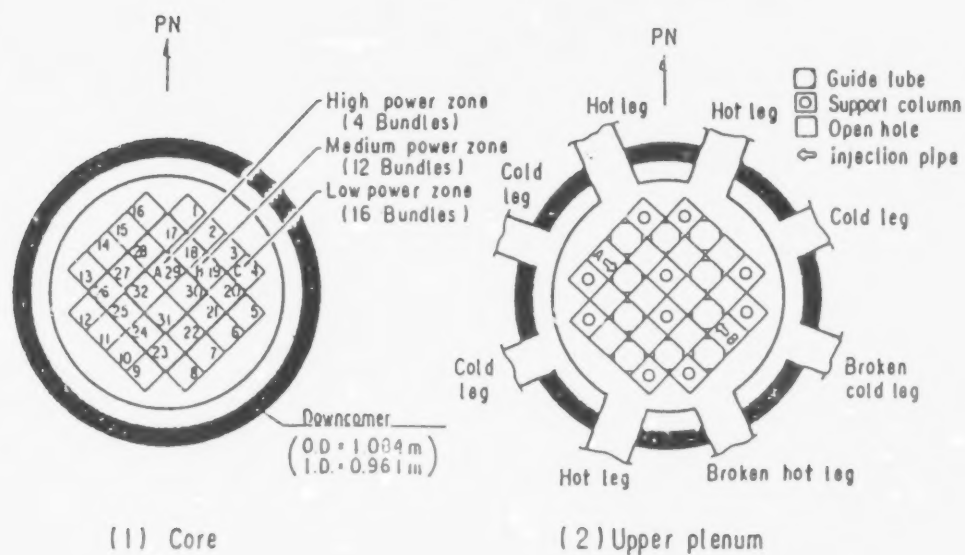


Fig. 2 Cross section of core and upper plenum in CCTF

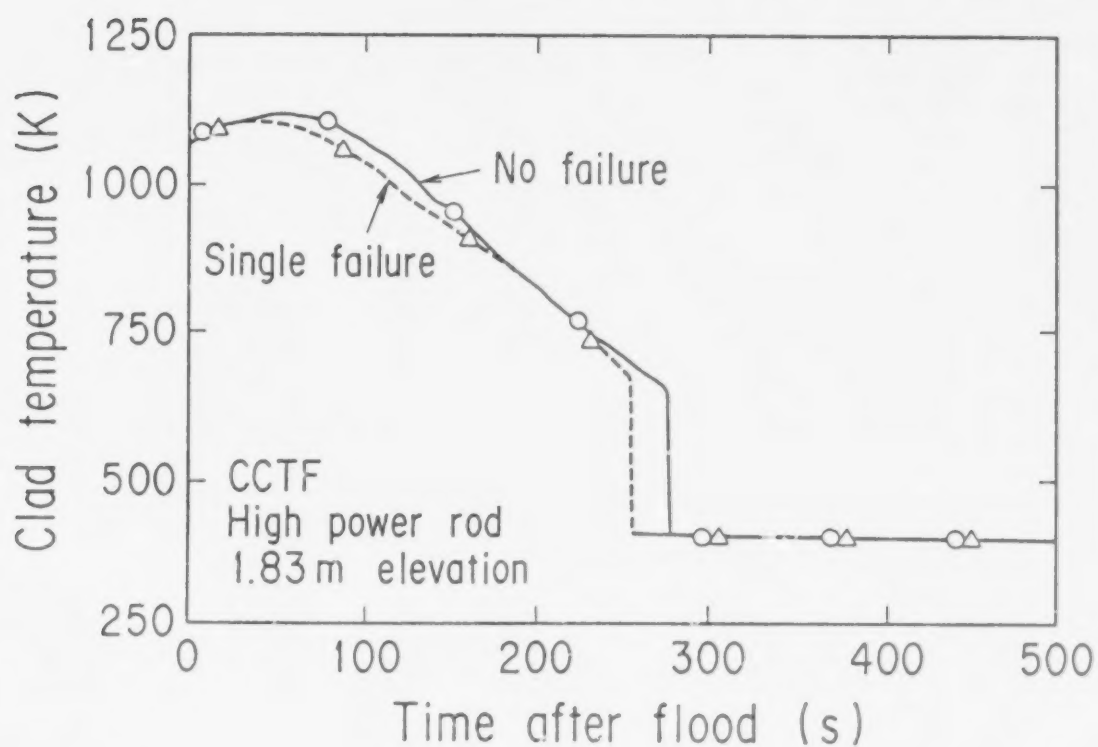


Fig. 3 Clad temperature under simulating no failure and single failure of ECCS

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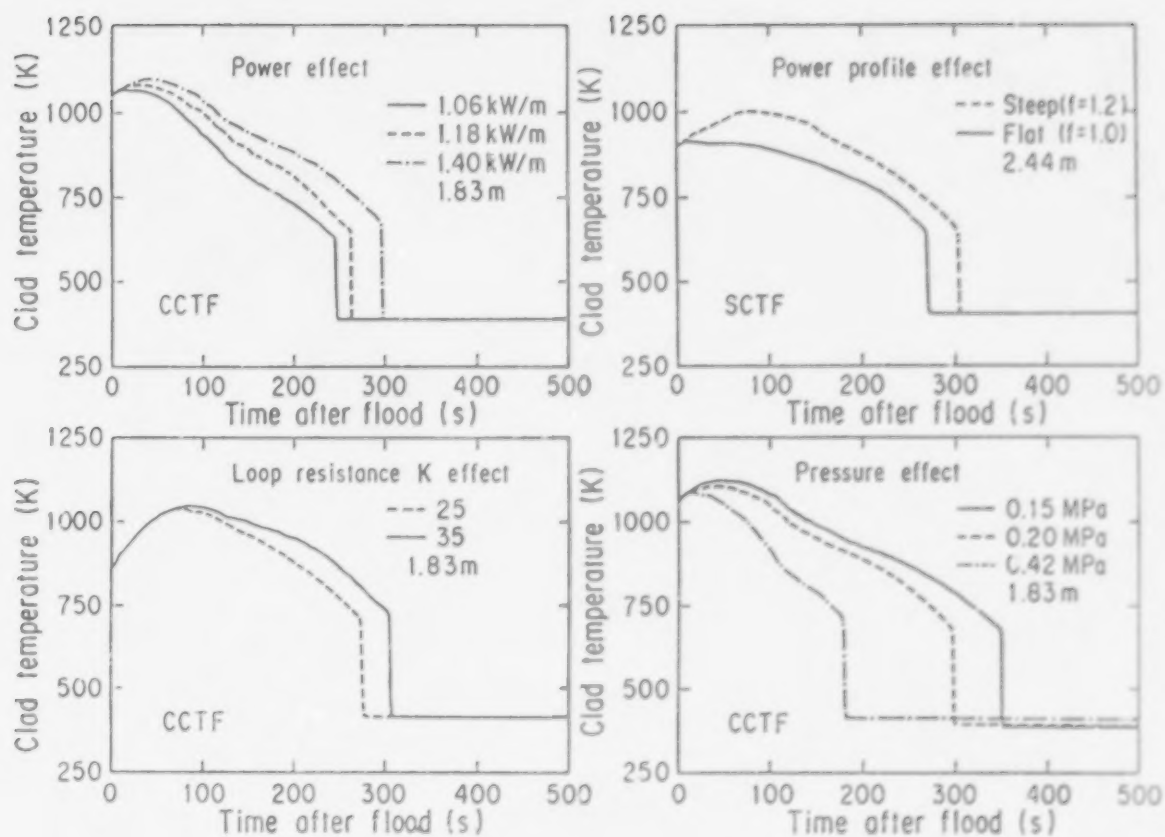


Fig. 4 Effect of parameters on clad temperature of high power rod

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WREM code

1. System calculation $\rightarrow$ Core boundary condition
2. Core calculation $\rightarrow$ PCT

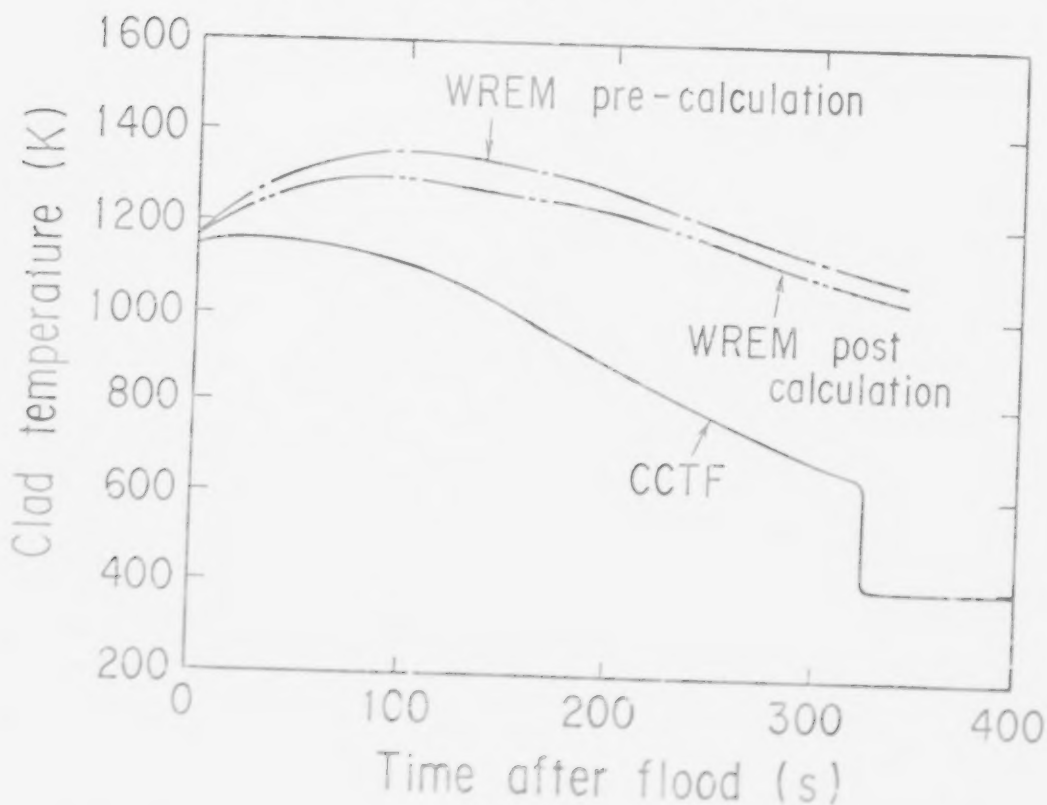


Fig. 5 Conservatism of prediction with WREM code on PCT

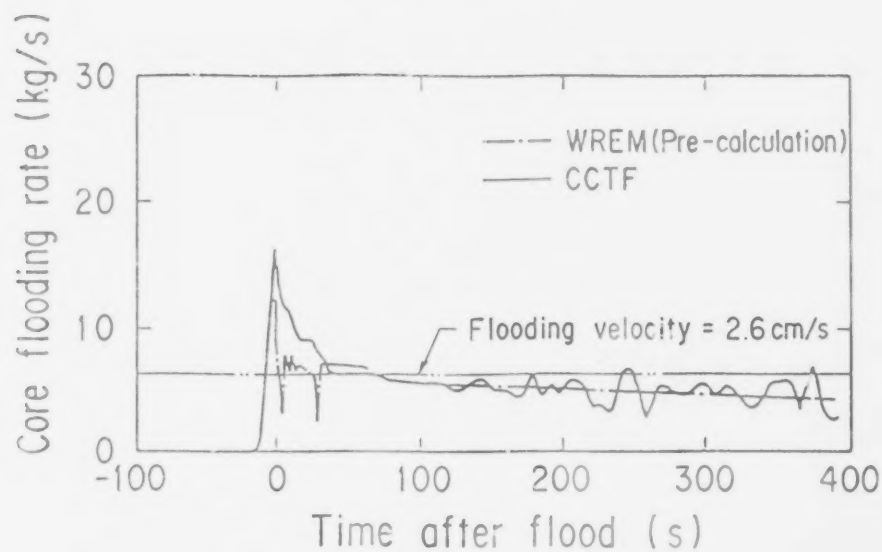


Fig. 6 Prediction on core flooding rate with WREM code

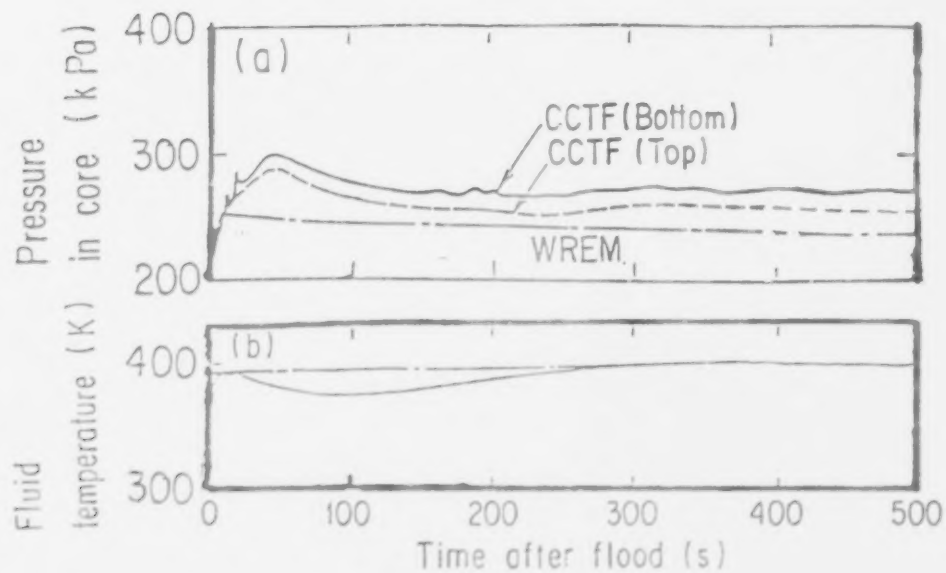


Fig. 7 Core inlet pressure and fluid temperature

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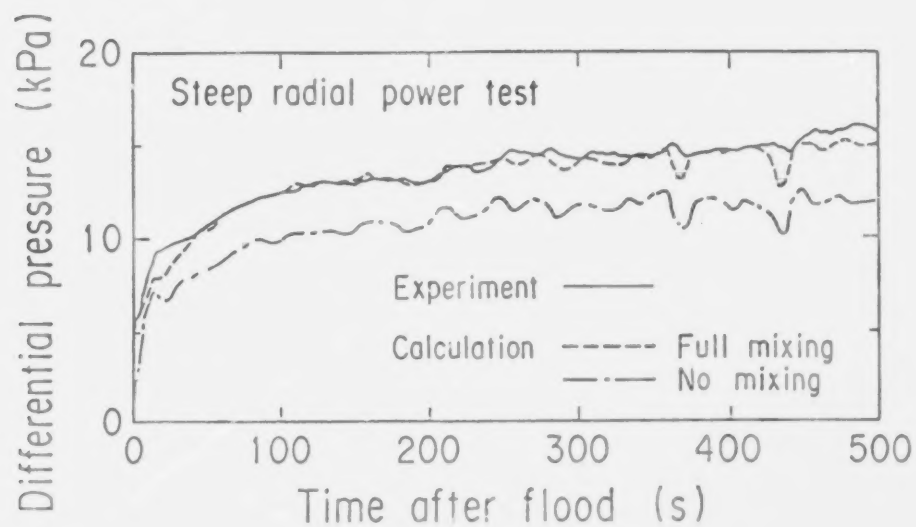


Fig. 8 Comparison of calculated core differential pressure with REFLA against data

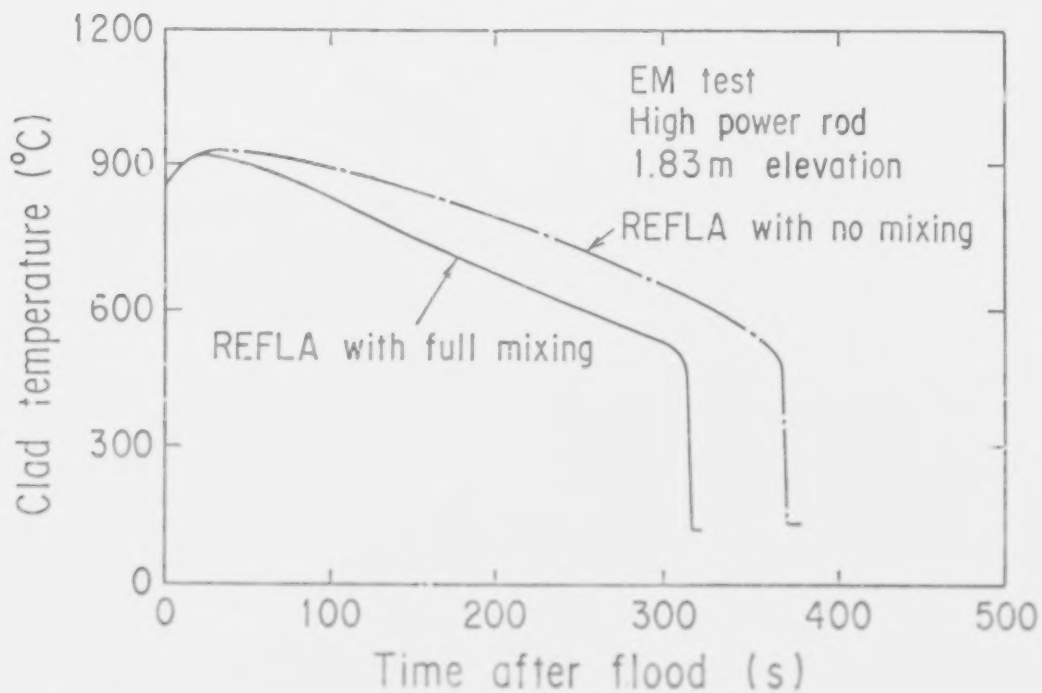


Fig.9 Estimated reduction of PCT due to fluid mixing model among subchannels

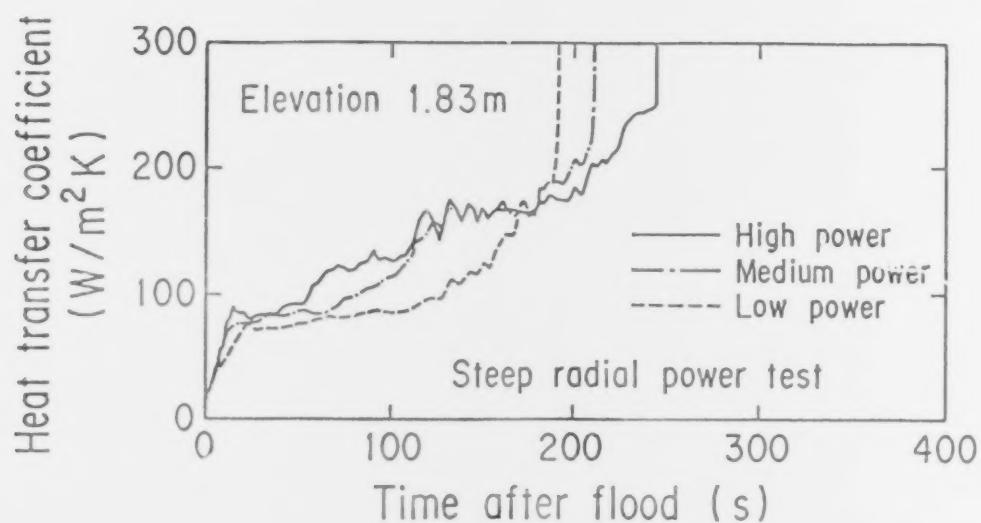


Fig.10 Experimental evidence of heat transfer enhancement in high power region

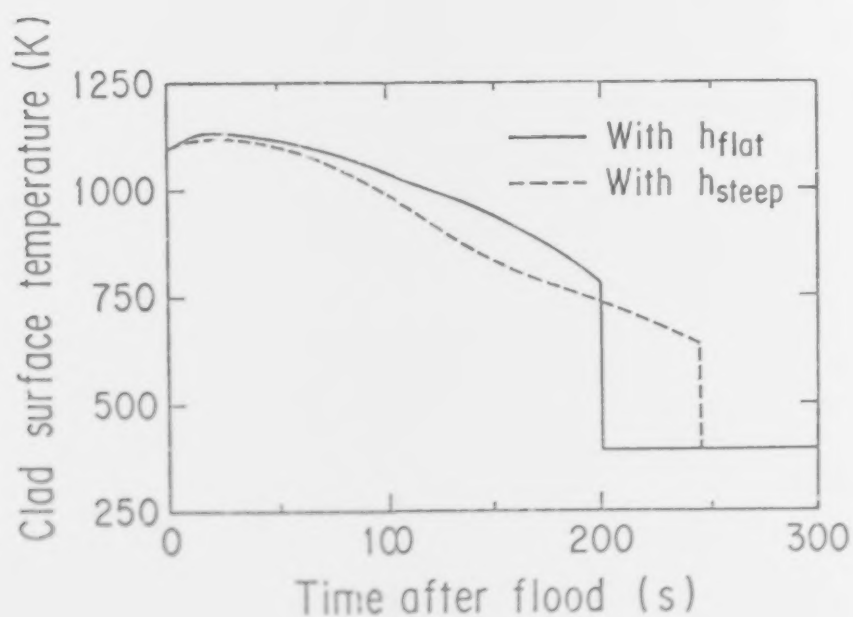


Fig.11 Estimated reduction of PCT due to power profile effect

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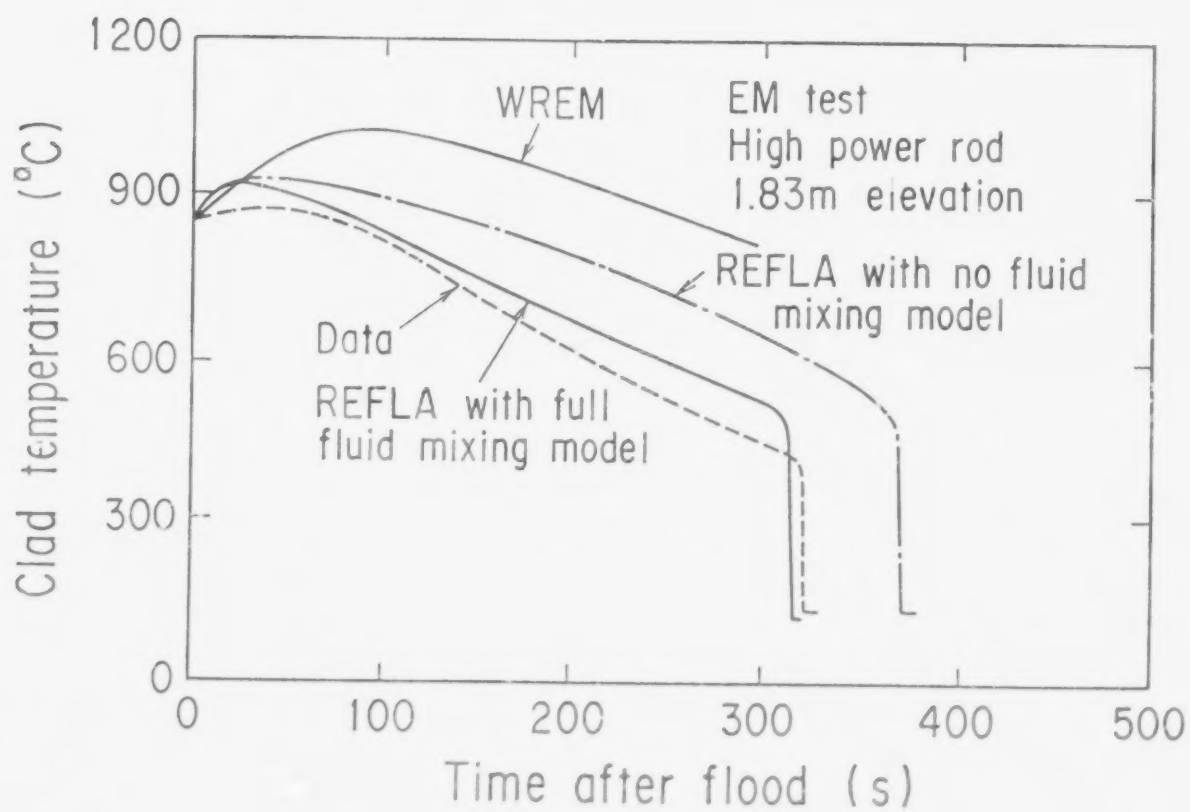


Fig. 12 Conservatism of WREM code

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SCTF-III test plan and recent SCTF-III test result

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abstract

Test plan of Slab Core Test Facility with Core-III (SCTF-III) has been clarified. The previous SCTF-III tests simulating PWRs with combined-injection-type ECCS indicated the following results on the thermal-hydraulics in full-radius core.

- (1) Two-region separation and multidimensional thermal-hydraulics both before and after reflood initiation
- (2) Practically no core cooling in the region far from water downflow region before reflood initiation in spite of good core cooling in water downflow region
- (3) Good core cooling even in two-phase upflow region after bottom reflood initiation
- (4) Large fall-back flow rate in comparison with the prediction by typical one-dimensional correlation

1. Introduction

A reflood test program⁽¹⁾ for a large-break-loss-of-coolant accident (LOCA) of pressurized water reactor (PWR) has been conducted at Japan Atomic Energy Research Institute (JAERI), by using large scale test facilities, which are the Cylindrical Core Test Facility (CCTF) and the Slab Core Test Facility (SCTF). This program has been in a part of 2D/3D project performed by USNRC, BMTF and JAERI.

The main objective of SCTF-I and SCTF-II tests is to study two-dimensional effect on thermal hydraulics in the full-radius core during reflood phase in a PWR with cold-leg-injection-type emergency core cooling system (ECCS). For this purpose, the SCTF was designed to have a full-length, full-radius and one-bundle width core and to distribute many kinds of instruments two-dimensionally in the pressure vessel.

The SCTF-I and SCTF-II tests⁽²⁾ have been completed by July, 1985 and the analytical activity is underway. Since January 1986, the SCTF test with Core-III (SCTF-III test) is being performed. In this presentation, the followings are explained.

The work was performed under contract with the Atomic Energy Bureau of Science and Technology Agency of Japan

- (1) SCTF-III test plan
- (2) Quick review of SCTF-III test results

2. Description of SCTF-III

SCTF was originally designed to study two-dimensional effect on thermal hydraulics during reflood phase in the core of full radius. Therefore, the SCTF has a full-length, full-radius and one-bundle width core to simulate the multi-dimensional thermal hydraulics in the core. The core flow area scaling ratio against typical 1300 MWe class PWR is 1/22.

The flow diagram is shown in Fig. 1. The SCTF is simulating a 200% cold-leg-break with a simplified primary system and can be operated at less than 0.6 MPa.

The vertical cross section of pressure vessel is shown in Fig. 2. The 1888 electrically heated rods simulating fuel rods are installed in the core. The dimension of a heated rod is 10.7 mm in diameter and 3613 mm in heated length. The axial peaking factor of a heated rod is 1.39. The maximum available power supplied into the core is 10 MW.

3. SCTF III-test plan

Special objectives of SCTF-III are follows.

- (1) For PWRs with cold-leg-injection-type ECCS
The objective is to investigate further two-dimensional thermal-hydraulics.
- (2) For PWRs with combined-injection-type ECCS
The objective is
 - (i) to obtain physical understanding on reflood phenomena,
 - (ii) to establish a quantitative prediction method on core cooling,
 - (iii) to provide data base for analytical codes.

The major effort will be concentrated to the above item (2) for PWRs with combined-injection-type ECCS. We will utilize the previous experimental information^{(3),(4)} obtained widely for PWRs with both cold-leg-injection-type and combined-injection-type ECCSs.

According to previous reflood tests for PWRs with combined-injection-type ECCS, the following features have been found on thermal hydraulics during reflood phase.^{(3),(4)}

- (1) CCF behavior with two-region separation (water downflow region and two-phase upflow region)
- (2) Significant multidimensionality of core cooling behavior (Rapid quenching in water downflow region and slow quenching in two-phase upflow region)
- (3) Net negative (downward) flow of coolant in core

The flow behavior is illustrated in Fig. 3. The above features are significantly different from that for PWRs with cold-leg-injection-type ECCS, in which the fluid flows upwards in core and the thermal hydraulic behavior is nearly one-dimensional. Accordingly, we think it is necessary to confirm the above (1)-(3) in a facility with full-radius core and quantify them, in order to attain the objectives

of item (2)(i) and (ii). Hence, we perform three groups of SCTF-III tests (core cooling tests, CCFL tests and integral tests). The accomplishment by data analysis will be incorporated into REFLA code⁽⁵⁾ to enable a quantitative prediction.

Our major interests for CCFL tests and core cooling tests are shown in Fig. 4.

4. Major results of previous SCTF-III tests

Up to present (End of July 1986), 12 SCTF-III tests have been performed. Those SCTF-III tests indicated the following results on the thermal hydraulics in full-radius core.

4.1 Core cooling tests

Test description

By simulating thermal hydraulics in PWRs with combined-injection-type ECCS, 6 core cooling tests were performed. Facility configuration for core cooling tests is illustrated in Fig. 5. ECC water is injected ununiformly into upper plenum only. Test sequence of one of them is shown in Fig. 6.

Result

Figure 7 shows ECC injection, differential pressures in downcomer, lower plenum, core, and upper plenum, and water accumulation in containment tank I, containment tank II and steam water separator. ECC water injected into upper plenum flows down through core into lower plenum and bottom part of downcomer before reflood initiation. After reflood initiation, ECC water accumulates in core and upper plenum in addition to downcomer. After filling-up of downcomer, ECC water flows into containment tank I and no ECC water flows into steam water separator. Thus, nearly all ECC water flows down through core, i.e. negative core flow was established for the core cooling test.

Figures 8 and 9 show the distribution of fluid temperature in core, and differential pressure across tie plate of end box. Time 140 s and 200 s correspond to the situations before and after reflood initiation, respectively.

These figures show that subcooled ECC water flows down locally in bundles 7 and 8 both before and after reflood initiation. Fluid velocity measured with turbine flow meters at upper core support plate holes indicated the fluid upflow in bundles 2, 4 and 6. Thus, formation of two region separation (water downflow region, bundles 7 and 8, and two-phase upflow region, probably bundle 1~5) was confirmed in the full-radius core.

Figures 10 and 11 show the clad temperature and heat transfer coefficient, respectively. In the water downflow region, even before reflood initiation a significant core cooling ($50 \sim 100 \text{ W/m}^2\text{K}$) is observed in spite of slight water accumulation in the region.

The water downflow enhanced the core cooling in the region (bundle 6) adjacent to the water downflow region. However, little

core cooling (practically no core cooling) is observed in the far region (especially bundles 1 and 3) before bottom reflooding.

After bottom reflood initiation, good core cooling is observed even in two-phase upflow region, although the multidimensional core cooling continues. Thus, the multidimensional core cooling was confirmed in full-radius core both before and after reflood initiation.

Figure 12 and 13 indicate the schematic of thermal hydraulics before and after reflood initiation for core cooling test. According to data shown in Fig. 8 and data with densitometer, it is suggested that nearly solid water column existed in water downflow region and incomplete condensation occurred (a small amount of steam flows towards the water downflow region).

4.2 CCFL tests

Test description

By simulating CCF behavior in PWRs with combined-injection-type ECCS, 3 CCFL tests were performed. Facility configuration for CCFL tests is illustrated in Fig. 14. Steam is fed by forced injection. Test sequence of one of CCFL tests is shown in Fig. 15.

Results

Figures 16 and 17 show the distributions of differential pressure across the plate of end box and fluid temperature above the plate. Formation of local water downflow (bundle 4) is indicated for 40 s, 260 s.

Figure 18 shows the liquid level in upper plenum. No significant distribution of liquid level in upper plenum is observed. Until 240 s, water remains in upper plenum, although water downflow increased with time (Fig. 19). At 240 s, significant water downflow occurs, which is noticed from sudden decreasing of liquid level in upper plenum. At this time, water downflow begins through entire cross section, as noticed from Fig. 16.

Figure 19 shows the estimated fall back flow rate through end box tie plate, obtained by summing up of measured water-accumulation in downcomer, lower plenum and core, and measured water-drainage from lower plenum bottom. Fall-back is estimated to start just at beginning of ECC injection into upper plenum. Fall-back flow rate increases with time.

Figure 20 shows the estimated steam upflow through end box tie plate. This steam upflow was estimated by forced steam injection rate-steam condensation rate in core due to subcooled fall-back water.

Figure 21 shows the relation between fall-back flow rate and steam upflow rate. In the figure, prediction with typical CCFL correlation, wallis correlation, is shown. Estimated fall-back flow rate is much higher than predicted one. Possible reasons are considered to be two-dimensional effect and subcooling effect.

Thermal hydraulics derived from CCFL test result is shown in Fig. 22. We think ununiform fall-back occurrence and small condensation (or little interaction between water downflow and steam upflow) as

interesting points. Small condensation (or incomplete condensation efficiency) was experimentally derived, since subcooling remains at core bottom.

5. Conclusions

Test plan of SCTF-III has been clarified. 12 SCTF-III tests have been performed. Following results on the thermal hydraulics in full-radius core have been indicated.

- (1) Two-region separation and multidimensional thermal-hydraulics was confirmed in full-radius core both before and after reflood initiation.
- (2) Practically no core cooling was observed in the region far from water downflow region before reflood initiation, although good core cooling was observed in water downflow region.
- (3) After bottom reflood initiation, good core cooling was observed even in two-phase upflow region, although the multidimensional core cooling remained.
- (4) Significantly large fall-back flow rate was observed in comparison with the prediction by typical one-dimensional correlation.

References

- (1) Hirano, K. and Murao, Y.: J. Nucl. Sci. Technol, 22(10), 681(1980).
- (2) Adachi, H., et al.: NUREG/CP-0048, 2, 277(1984).
- (3) Murao, Y., et al.: Thirteenth Water Reactor Safety Research Information Meeting, Gaithersburg (1985)
- (4) Watzinger, H., et al.: Second International Topical Meeting on Nuclear Power Plant Thermal Hydraulics and Operation, Tokyo (1986).
- (5) Murao, Y., et al.: JAERI-T 84-243 (1984).

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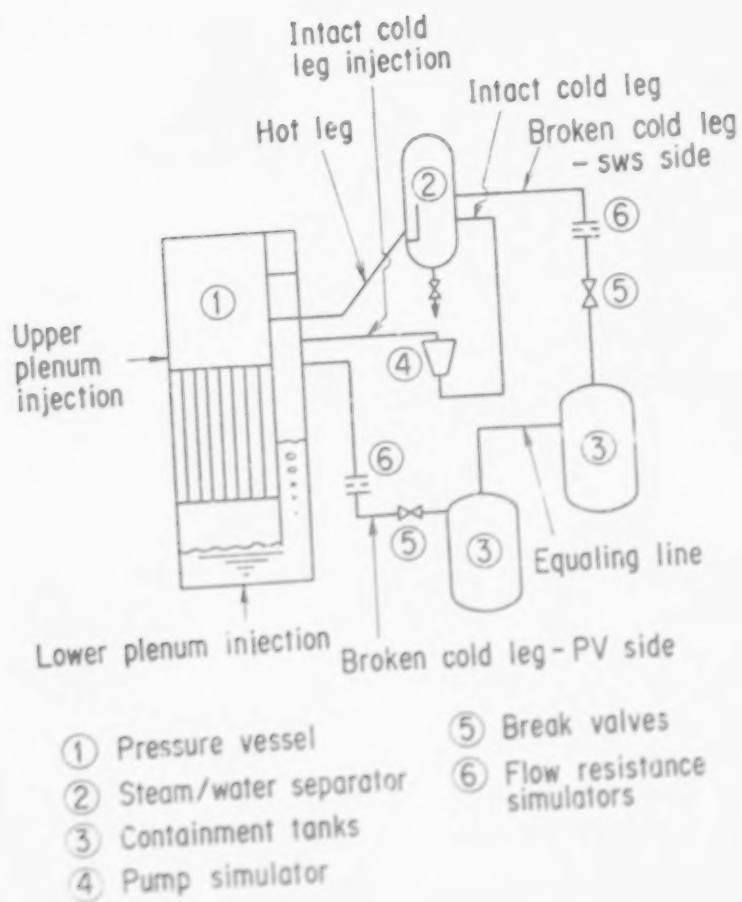


Fig. 1 Flow diagram of SCTF

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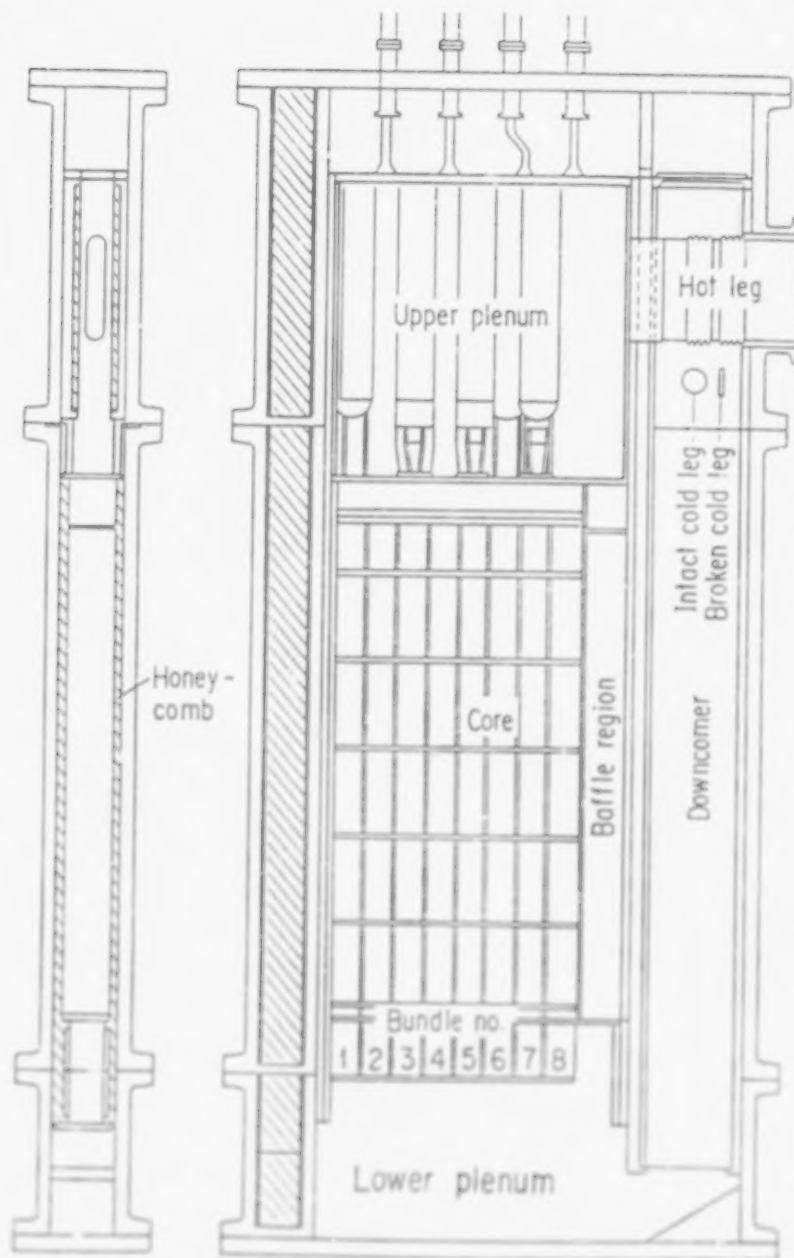
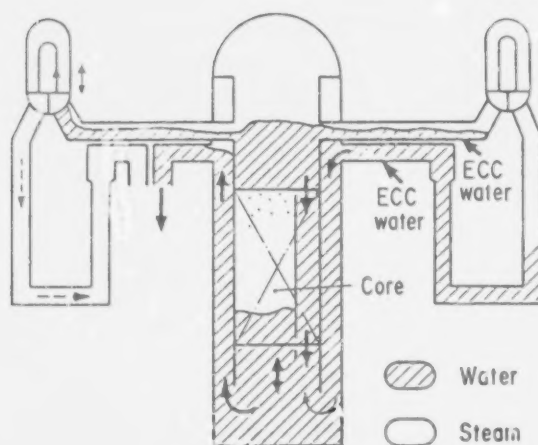


Fig. 2 Vertical cross section of pressure vessel

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- (1) CCF behavior with two-region separation
 - (2) Significant multidimensionality of core cooling behavior
 - (3) Net negative (downward) fluid flow in core
- (Experimental result of CCTF)

Fig. 3 Features of thermal hydraulics during reflood phase in PWRs with combined-injection-type ECCS

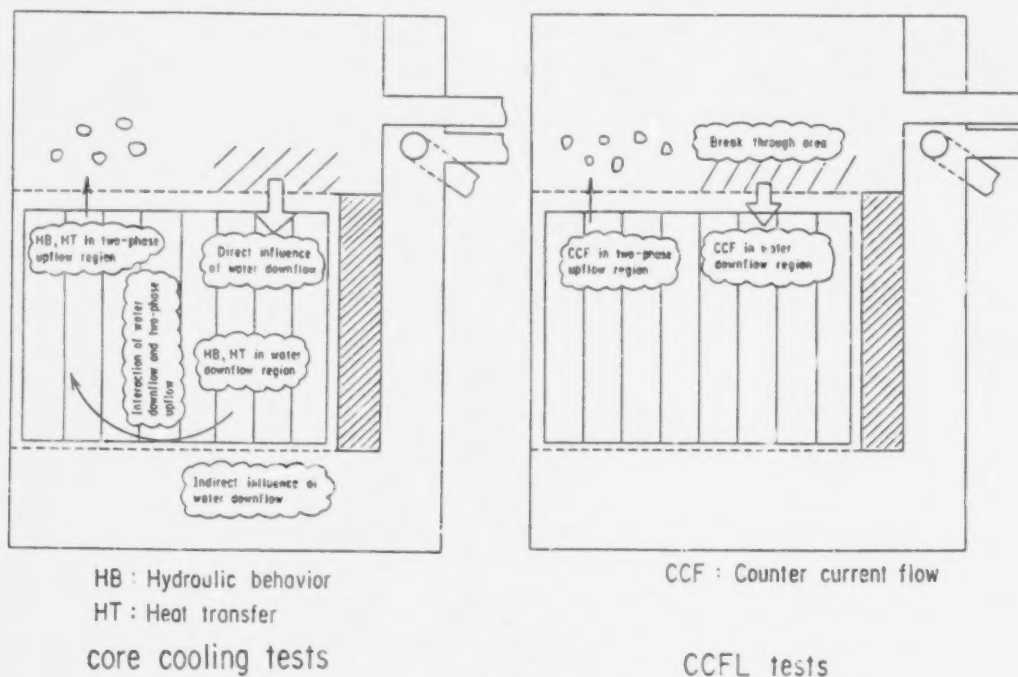


Fig. 4 Interesting points for core cooling tests and CCFL tests

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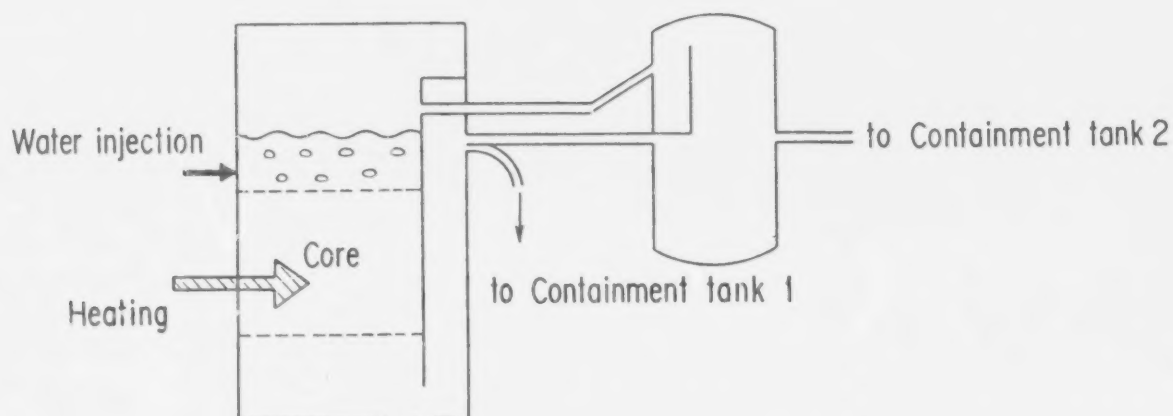


Fig. 5 Facility configuration for core cooling tests

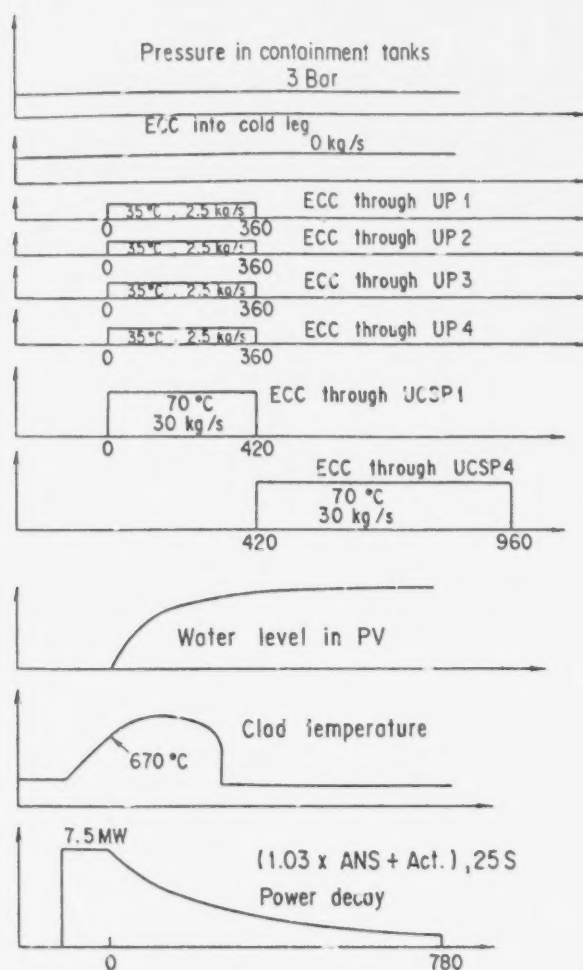


Fig. 6 Test sequence of core cooling test (S3-SH1)

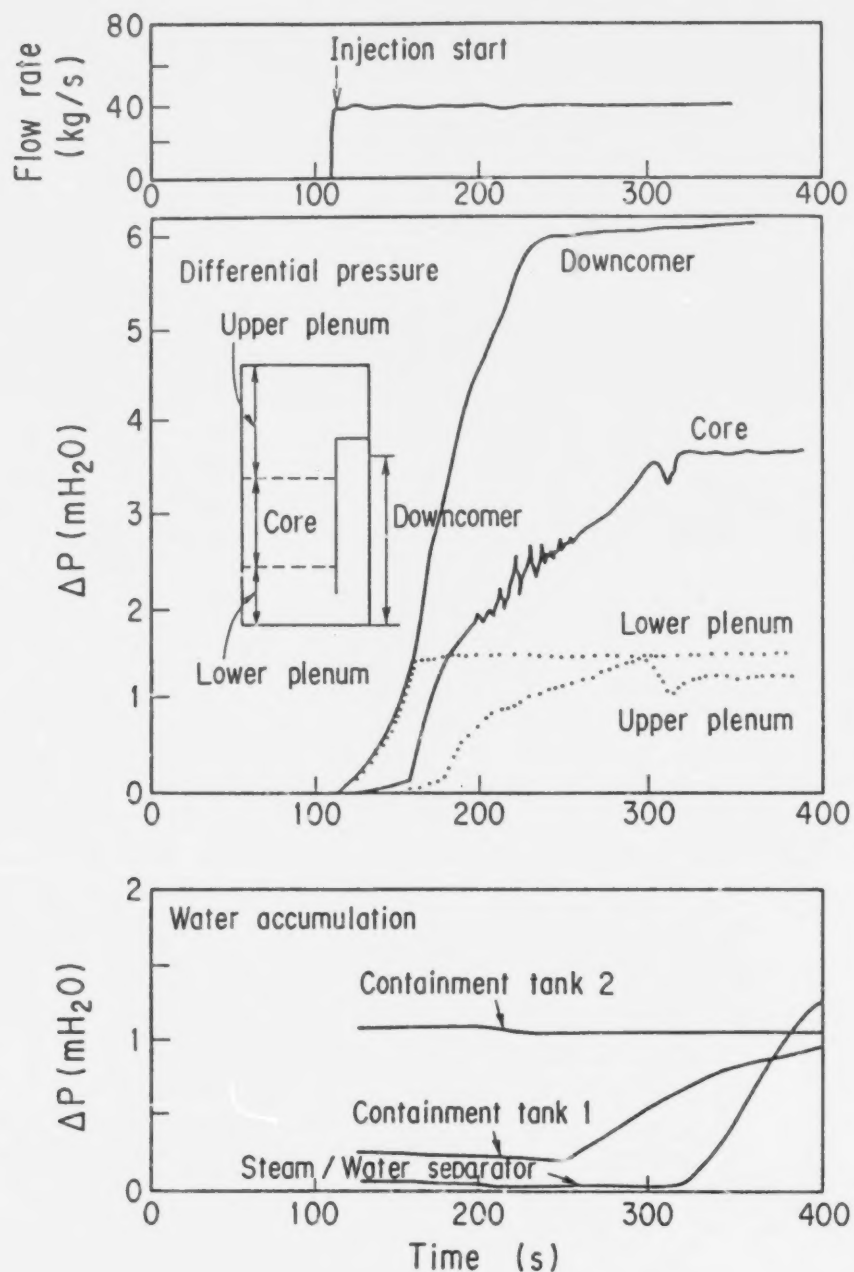


Fig. 7 Mass distribution for core cooling test (S3-SH1)

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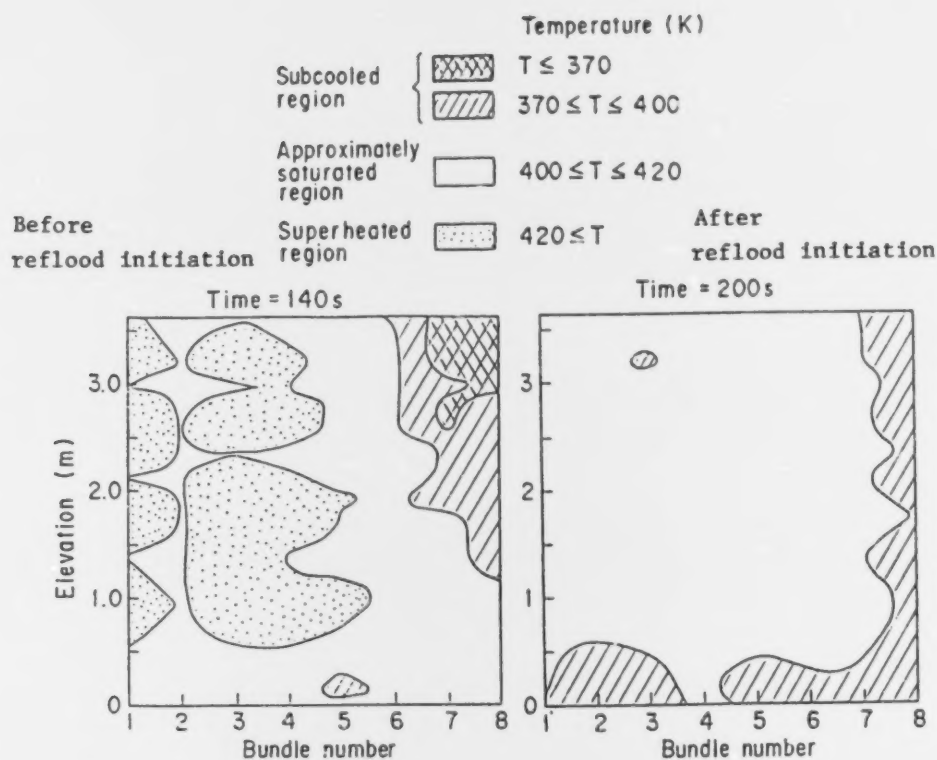


Fig. 8 Distribution of fluid temperature for core cooling test (S3-SH1)

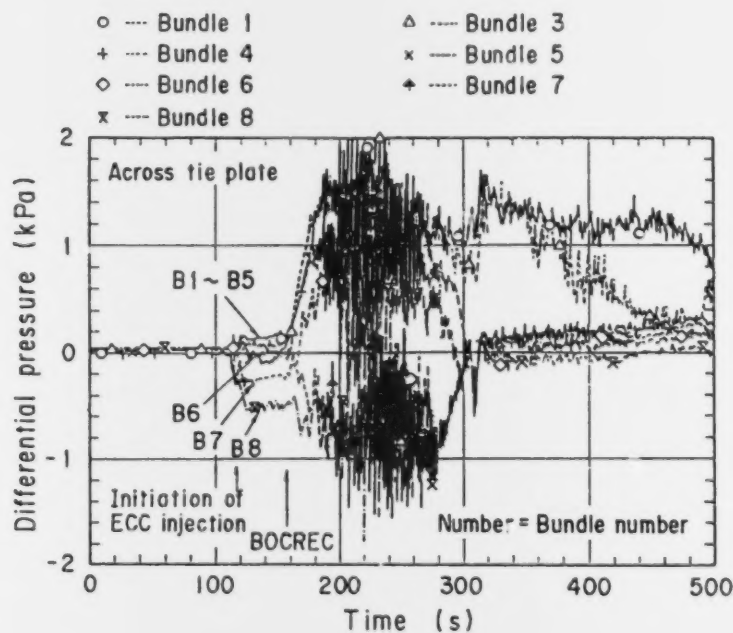


Fig. 9 Distribution of differential pressure across end box for core cooling test (S3-SH1)

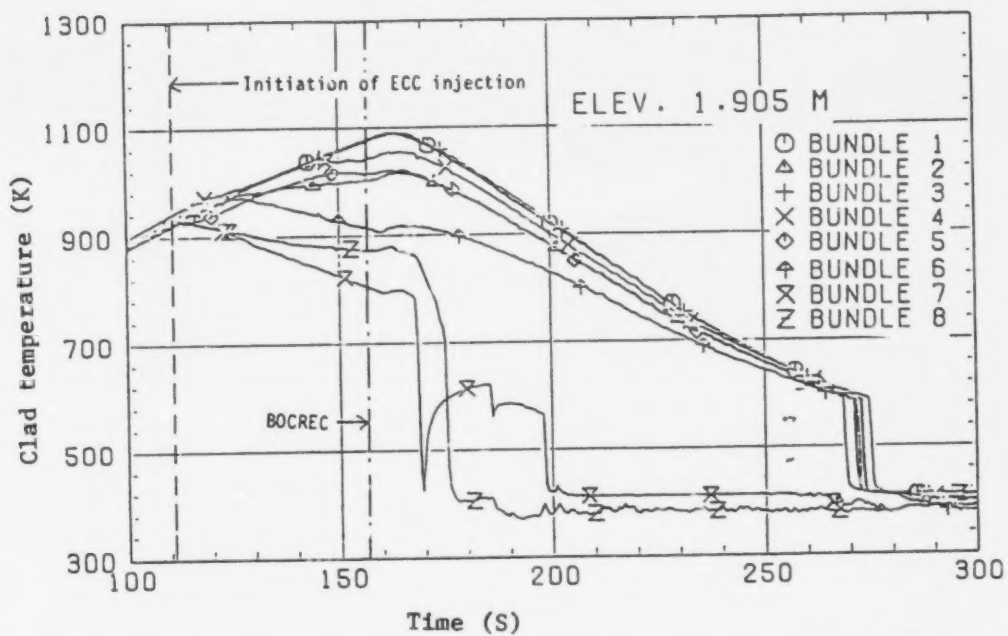


Fig.10 Clad temperature for core cooling test (S3-SH1)

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TEST S3-SH1
ELEV. 1.905 M

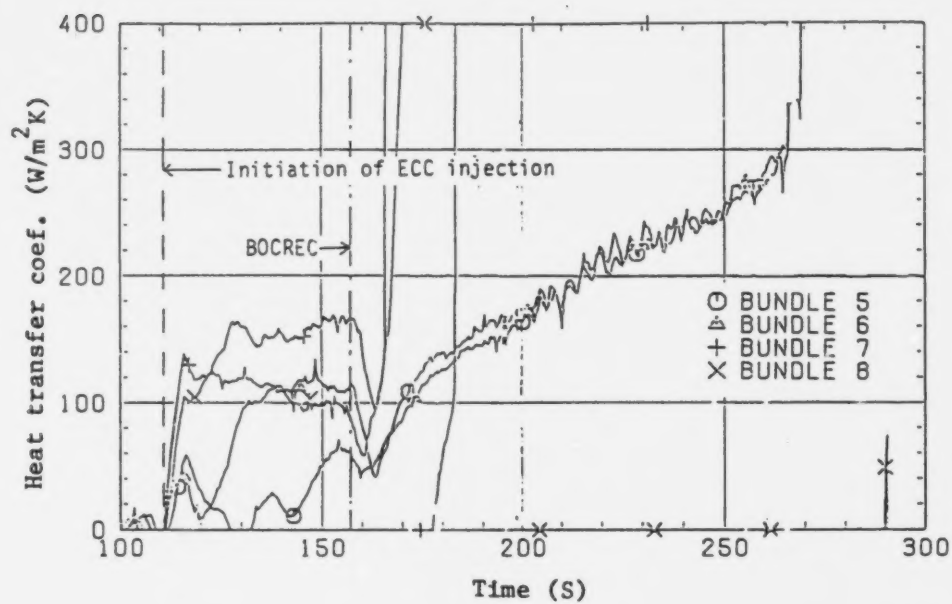
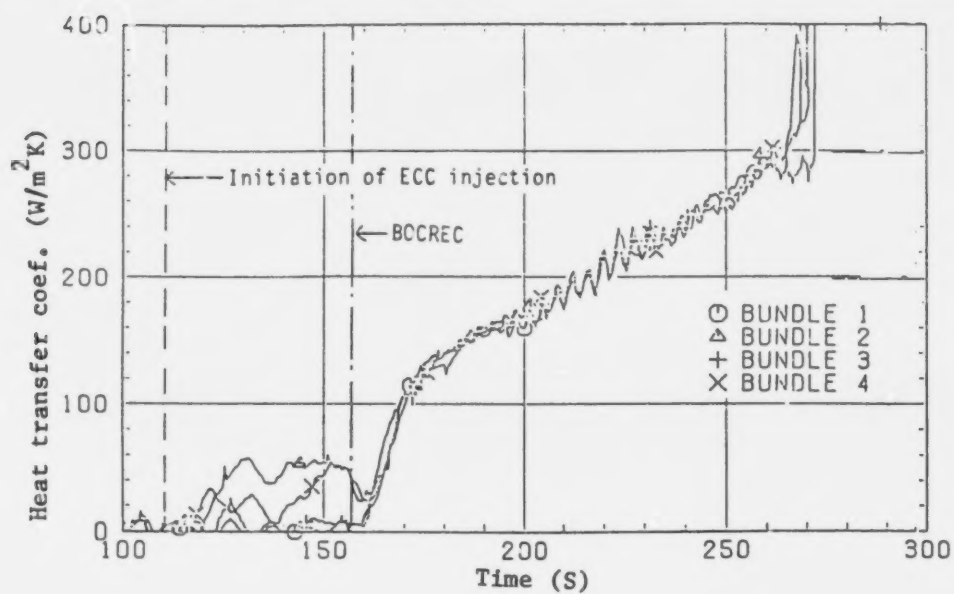


Fig.11 Heat transfer coefficient for core cooling test (S3-SH1)

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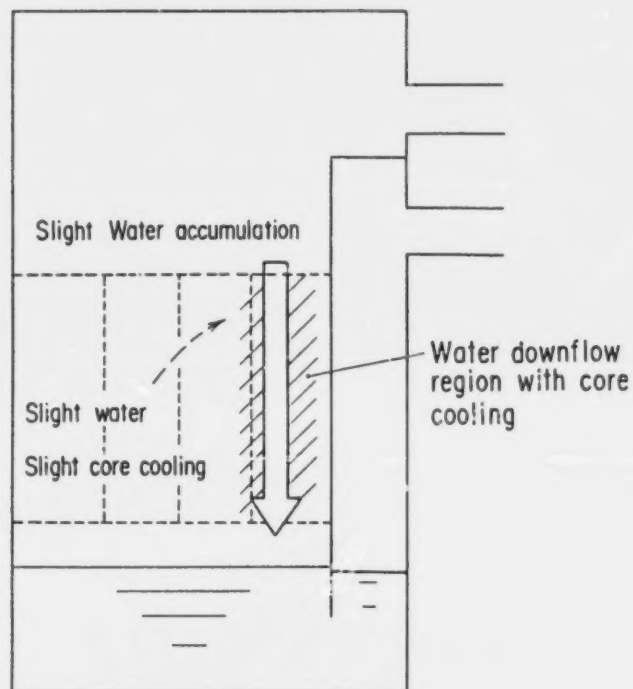


Fig.12 Thermal hydraulics before reflood initiation for core cooling test

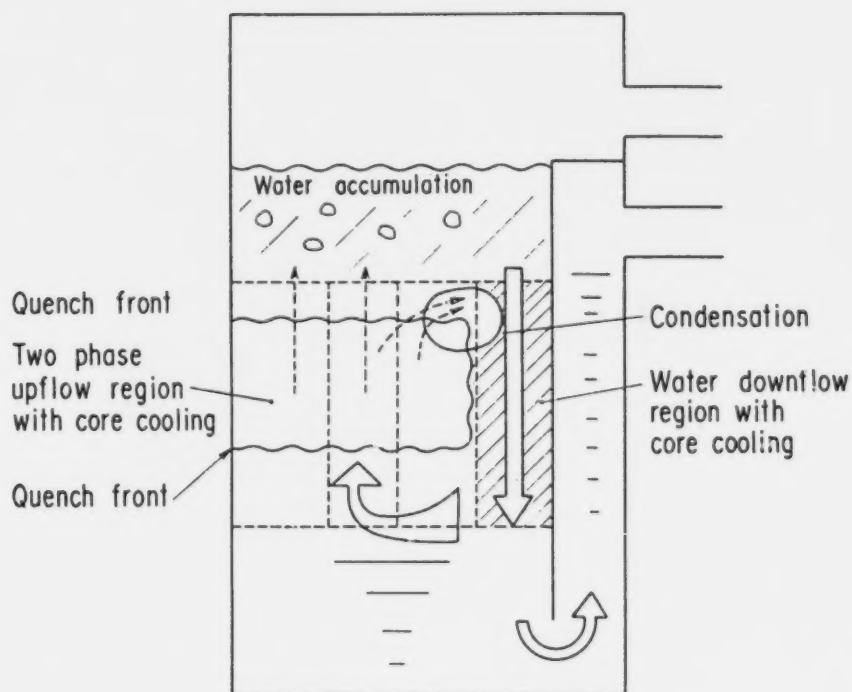


Fig.13 Thermal hydraulics after reflood initiation for core cooling test

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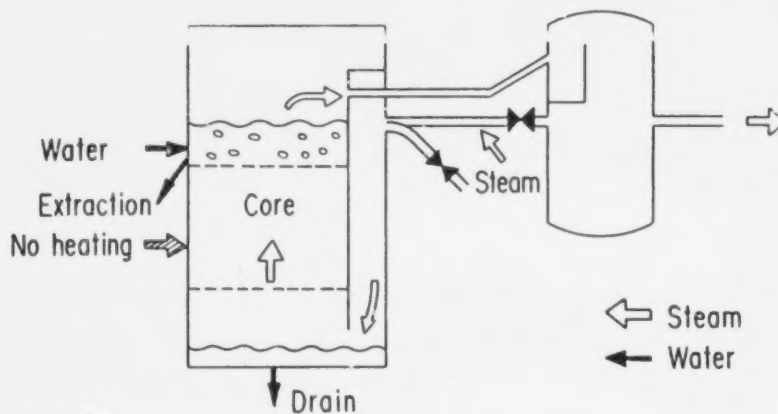


Fig.14 Facility configuration for CCFL tests

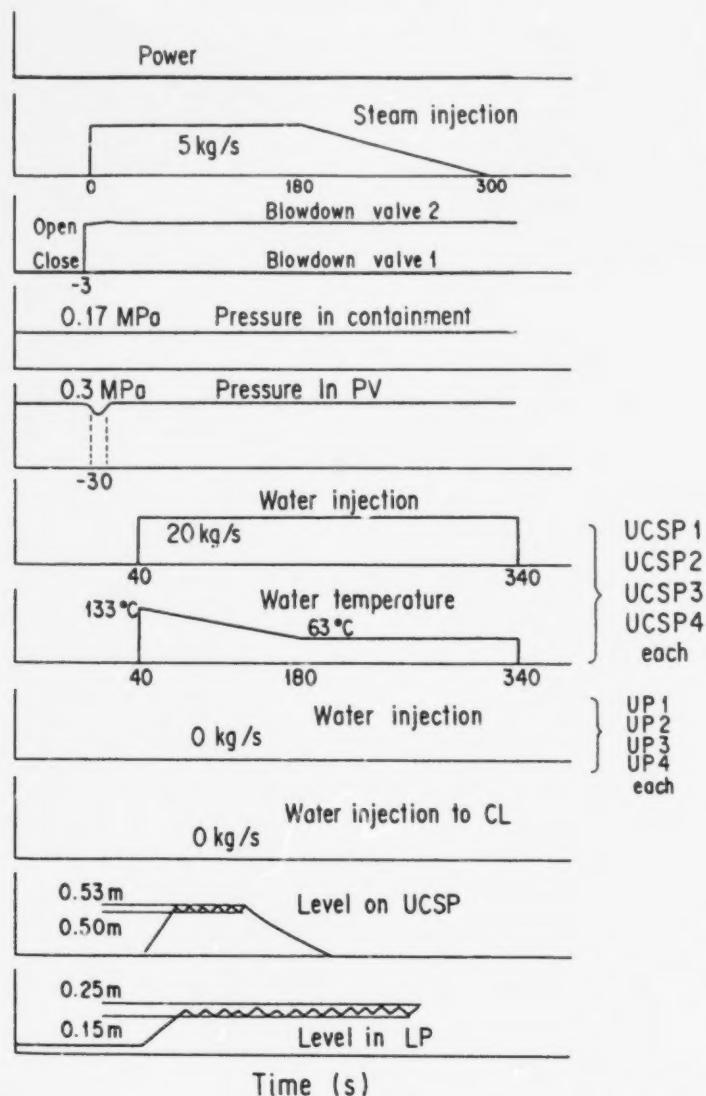


Fig.15 Test sequence for CCFL test (S3-3)

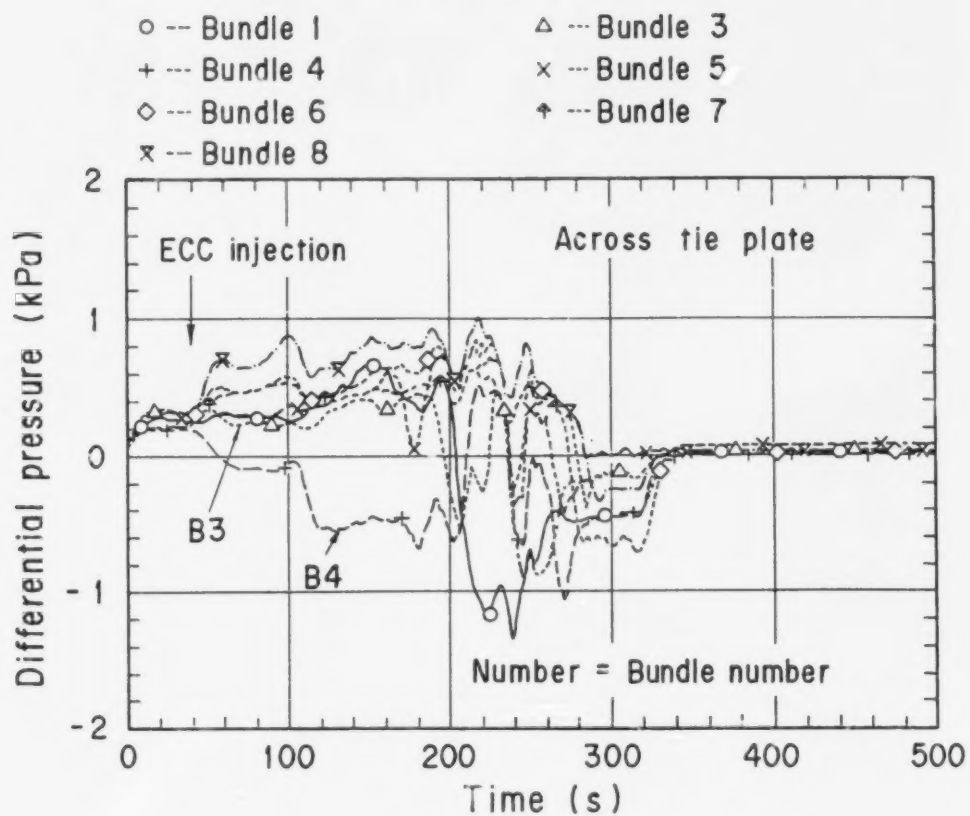


Fig.16 Differential pressure across end box for CCFL test (S3-3)

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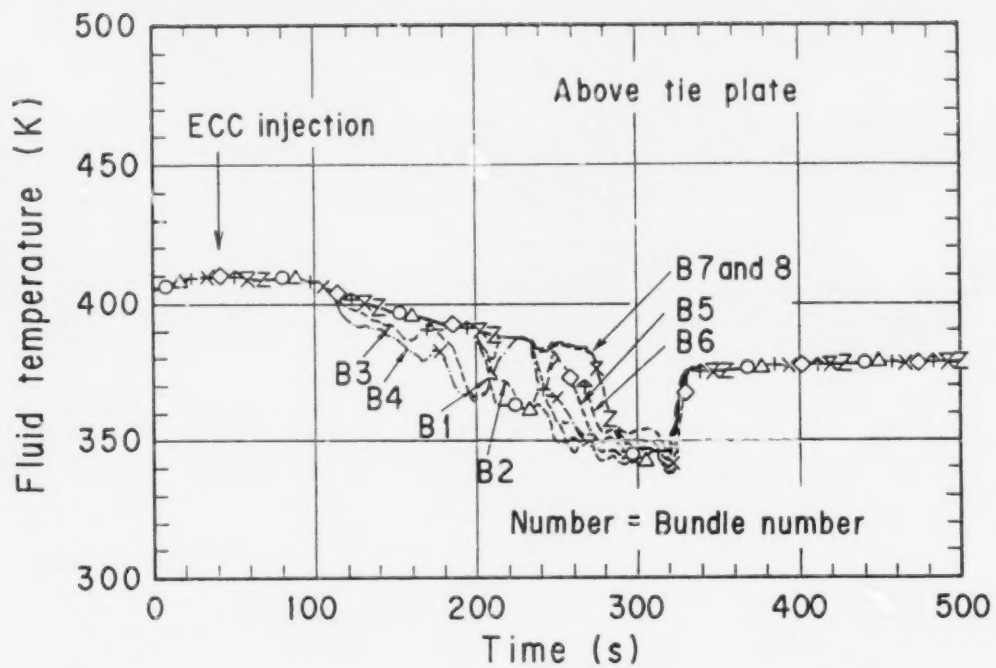


Fig.17 Fluid temperature above tie plate for CCFL test (S3-3)

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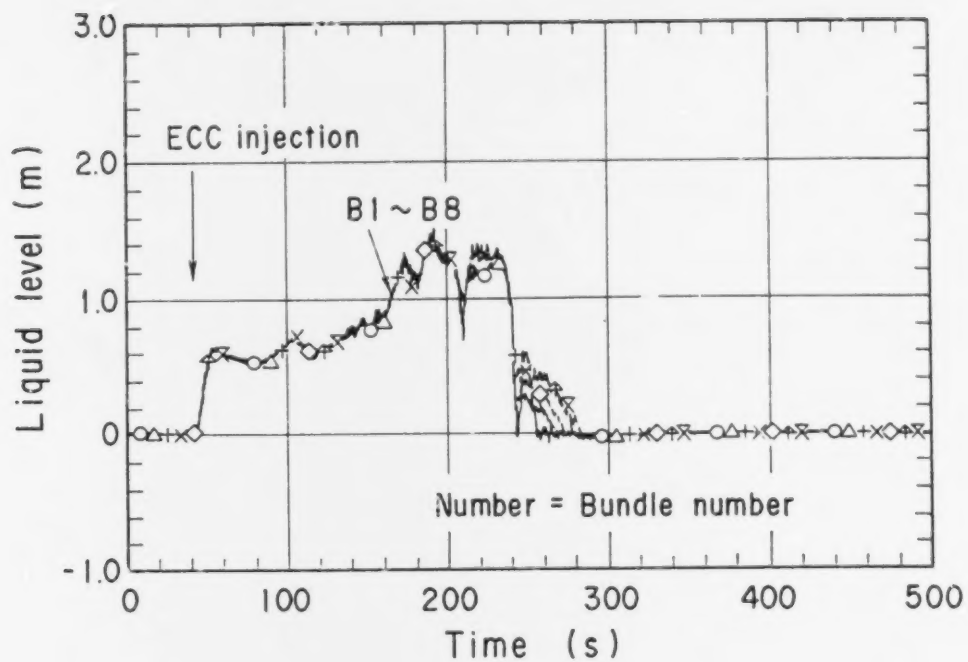


Fig.18 Liquid level in upper plenum for CCFL test (S3-3)

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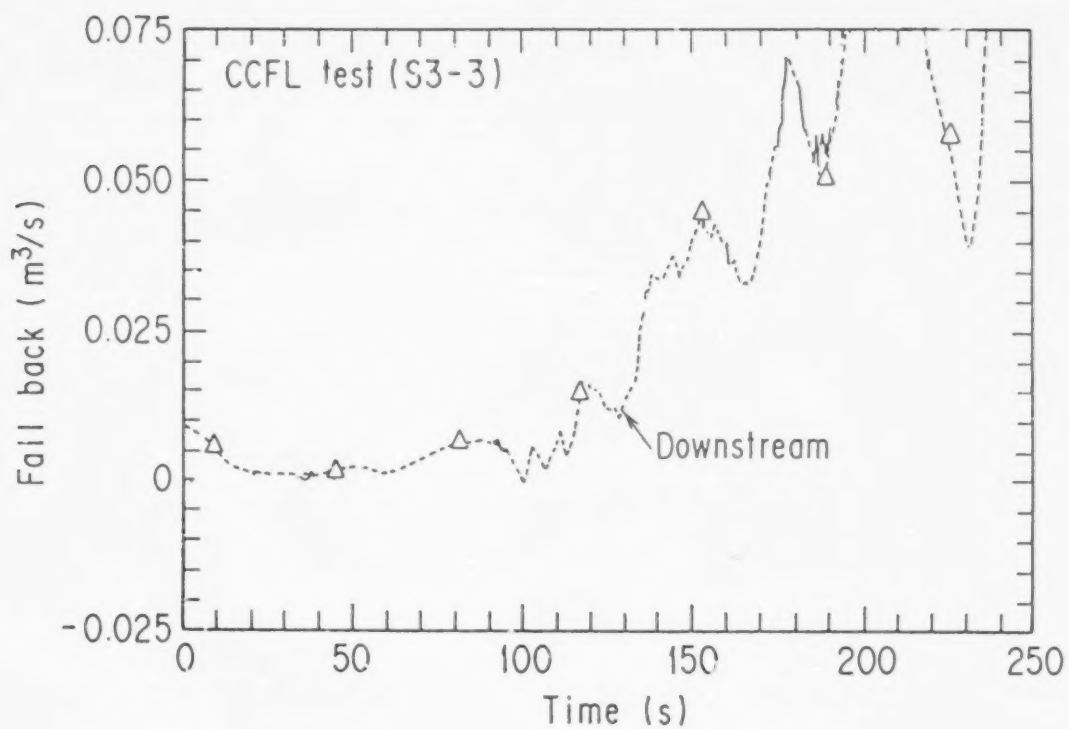


Fig.19 Estimated fall back flow rate for CCFL test (S3-3)

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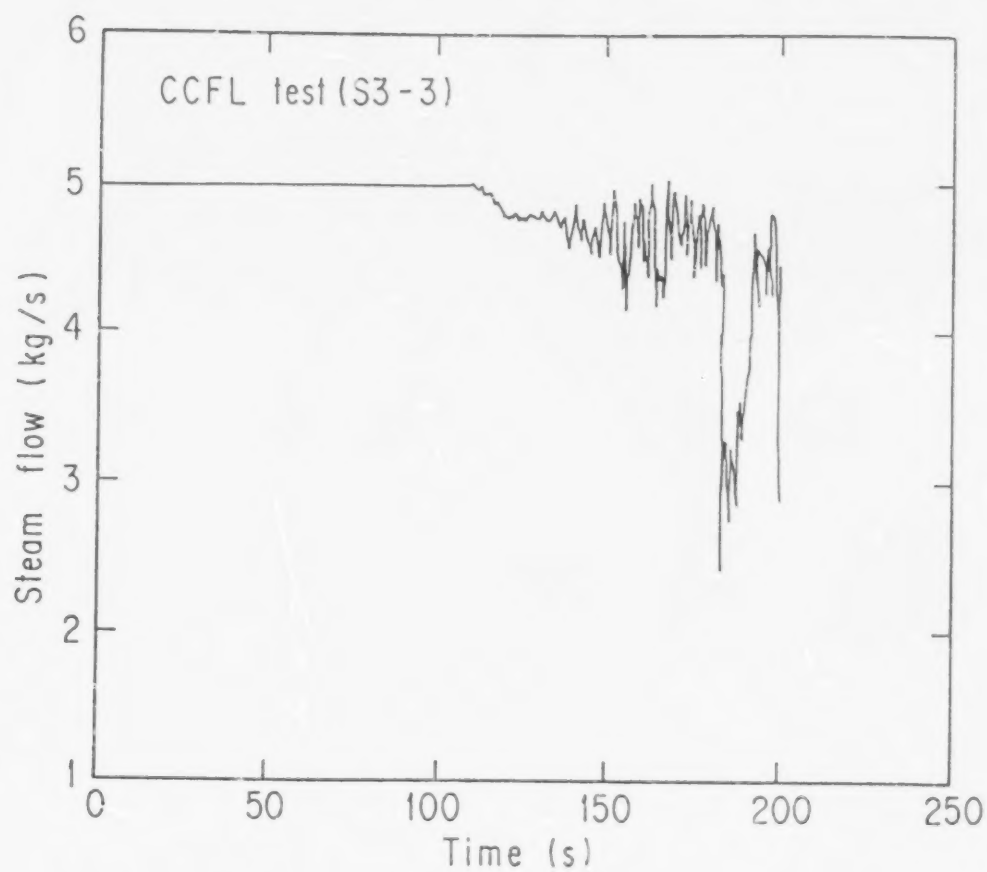


Fig.20 Steam flow rate at tie plate for CCFL test (S3-3)

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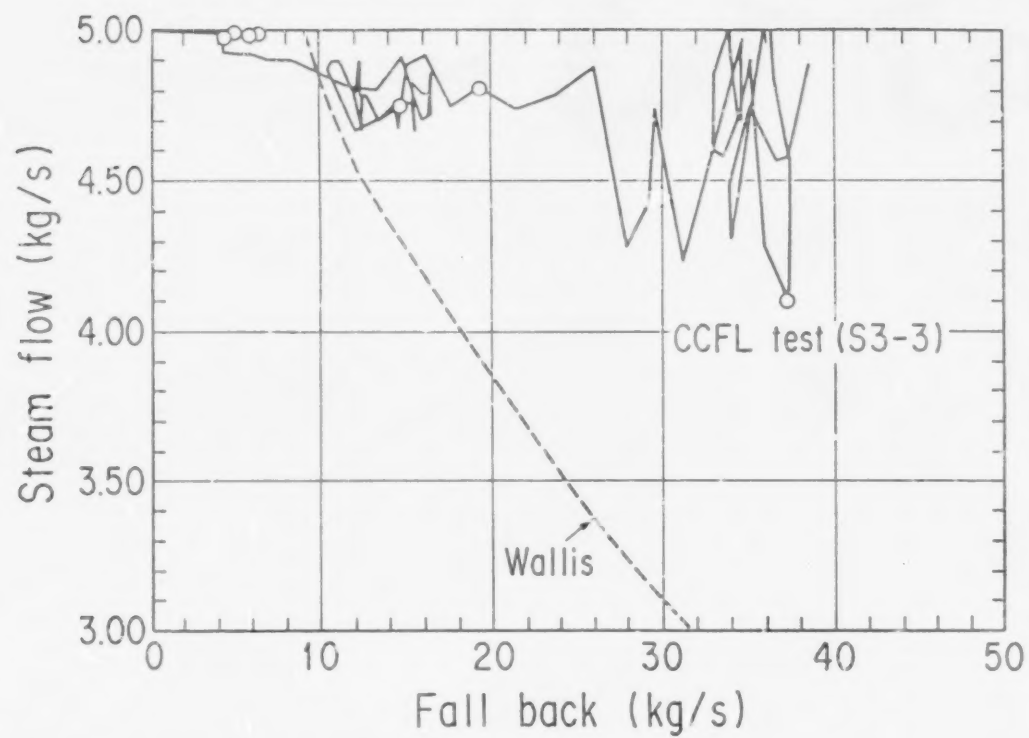


Fig.21 Relation of fall back flow and steam upflow for CCFL test (S3-3)

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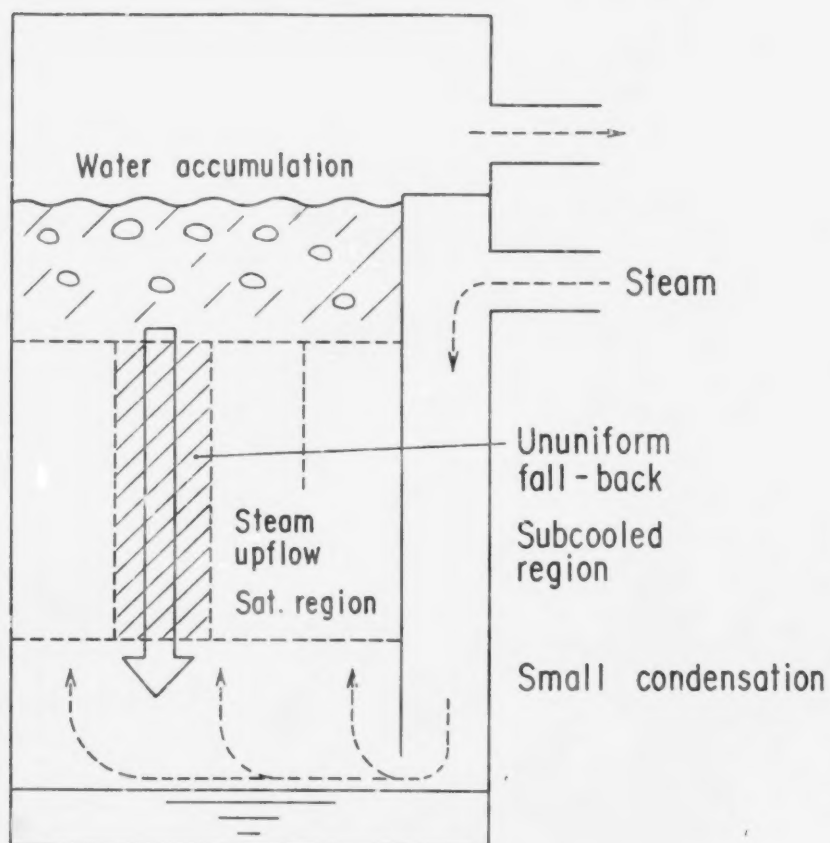


Fig.22 Thermal hydraulics for CCFL test

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HEAT TRANSFER ENHANCEMENT IN SCTF TESTS

Takamichi IWAMURA, Hiromichi ADACHI, Makoto SOBAJIMA,
Akira OHNUKI, Tsutomu OKUBO, Yutaka ABE and Yoshio MURAO

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Abstracts

The reflood tests using the Slab Core Test Facility(SCTF) of Japan Atomic Energy Research Institute(JAERI) indicated that the radial power distribution induced the heat transfer enhancement above the quench front for higher power bundles. The two-dimensional distribution of heat transfer coefficient was closely related to the two-dimensional distribution of normalized local pressure drop. The Bromley type film boiling heat transfer correlation was not able to predict the two-dimensional heat transfer behavior. Based on a dispersed flow model, the heat transfer coefficient was expressed as the sum of three terms; radiation, droplet impingement, and forced convection of steam. The heat transfer coefficients calculated with this model agreed well with the data from the SCTF, the Cylindrical Core Test Facility(CCTF) and the FLECHT-SEASET when the liquid fraction was less than 0.1. The heat transfer enhancement for higher power bundles due to the radial power distribution was predicted well with this model.

1. Introduction

The SCTF (Slab Core Test Facility) Program is a part of the Large Scale Reflood Test Program together with the CCTF (Cylindrical Core Test Facility) Program. The major objective of the SCTF Program is to investigate two-dimensional thermal-hydraulic behavior in the core during the reflood phase of a loss-of-coolant accident (LOCA) of a pressurized water reactor (PWR). In order to meet this objective, SCTF simulates a full radius slab section of a PWR.

It was revealed from the SCTF test results that heat transfer was enhanced for the higher power bundles and degraded for the lower power bundles^{(1),(2)}. Resultantly, the peak cladding temperature was lower in the steep radial power distribution test as compared with that in the flat radial power distribution test under the same total heating power and initial core stored energy^{(1),(2)}. This two-dimensional heat transfer characteristic may be caused by the radial distribution of local hydraulic conditions such as liquid fraction and steam and liquid velocities. However, the physical mechanism of the heat transfer enhancement for high power bundles had not been clarified. In order to clarify this mechanism, the difference in the heat transfer coefficients among bundles should be quantitatively described by a suitable heat transfer correlation based on the local hydraulic conditions.

In the present study, the relation between the heat transfer coefficient and the normalized local pressure drop is at first investigated using the SCTF test data. Then, a dispersed flow heat transfer model is presented and compared with experimental results. The mechanism of the heat transfer

The work was performed under contract with the Atomic Energy Bureau of Science and Technology Agency of Japan.

enhancement for high power bundles due to the radial power distribution is discussed based on the calculated results.

2. Experiment

2.1 Facility

Figure 1 shows a vertical cross section of the SCTF pressure vessel. The pressure vessel includes a simulated core, an upper plenum with internals, a lower plenum, a core baffle and a downcomer. The SCTF pressure vessel simulates a full radius slab section of a 1,100 MWe class PWR. The simulated core consists of 8 bundles arranged in a row and the heating power for each bundle can be independently controlled during a test so as to investigate the effects of radial core power distribution. Bundle 1 corresponds to the center bundle and Bundle 8 to the peripheral bundle of a PWR. Each bundle consists of 234 heated rods and 22 non-heated rods arranged in a 16×16 array with 14.3 mm pitch. The outer diameter and the heated length of the heated rod are 10.7 and 3,660 mm, respectively. The flow area scaling ratio is 1/21 of a 1,100 MWe class PWR, whereas the heights of each component are preserved.

2.2 Test Conditions

Major test conditions for the tests referred to in this study are listed in Table 1. The radial power distribution was flat in Test S2-14 and steep in Test S2-12 under the same total power and initial core stored energy. In these two tests, the downcomer was isolated from the lower plenum and emergency core cooling (ECC) water was directly injected into the lower plenum. The water in the upper plenum was extracted so as to avoid the effect of nonuniform water accumulation in the upper plenum.

3. Experimental Results

3.1 Radial Distribution of Heat Transfer Coefficient

Figure 2 shows the comparison of heat transfer coefficients between Bundles 4 and 8 at 2.33 m from the bottom of heated part with respect to the distance from the bottom-up quench front for the steep and flat radial power distribution tests. The heat transfer above the quench front is enhanced in the high power bundle (Bundle 4) and degraded in the low power bundle (Bundle 8) for the steep radial power distribution test (S2-12), while the difference between bundles is small for the flat radial power distribution test (S2-14).

3.2 Radial Distribution of Normalized Local Pressure Drop

The measured vertical pressure drops at 6 elevations in the SCTF core are the sum of gravitational, frictional and accelerational pressure drops. Here, a normalized local pressure drop is defined as the ratio of the measured pressure drop to the gravitational pressure drop for single-phase water across the measurement span. Thus, the normalized pressure drop includes the effect of steam velocity in addition to the effect of liquid fraction.

Figure 3 shows the comparison of the normalized local pressure drops in Bundles 2, 4 and 8 at elevation 2.33 m for Tests S2-12 and S2-14. The

normalized pressure drop is the highest in the high power bundle (Bundle 4) and the lowest in the low power bundle (Bundle 8) for a given distance from the bottom-up quench front in Test S2-12, while no significant difference among bundles is observed in Test S2-14. This relation is quite similar to the relation between the heat transfer coefficient and the distance from the bottom-up quench front shown in Fig. 2.

The higher vertical pressure drop in the high power bundle is explained to be caused mainly by the higher liquid fraction in these bundles due to the following three effects;

- 1) The entrainment generation rate around the quench front is larger due to the higher cladding temperature,
- 2) A natural convection below the quench front is induced by the radial distribution of steam generation rate, and
- 3) The concave distribution of the quench front enhances the water concentration into the high power bundles.

The higher steam velocity in the high power bundle also contributes to the higher vertical pressure drop in these bundles.

3.3 Relation between Heat Transfer Coefficient and Normalized Pressure Drop

The heat transfer coefficients in Bundles 2, 4 and 8 for Tests S2-12 and S2-14 at elevation 2.33 m are plotted against the normalized pressure drops in Fig. 4. As shown in this figure, the heat transfer coefficient above the quench front is strongly dependent on the normalized pressure drop and the radial power distribution has little effect on this relation.

In order to clarify the causal relation of the distributions of heat transfer coefficients and normalized pressure drops in the steep radial power distribution test (S2-12), the following procedure was adopted. At first, an empirical relation of the heat transfer coefficient with respect to the normalized pressure drop was obtained from the flat power distribution test (S2-14). Then, the heat transfer coefficient in Test S2-12 was estimated by applying this relation to the normalized pressure drop in this test. The estimated heat transfer coefficients in Bundles 2, 4 and 8 at elevation 2.33 m are compared with the measured ones in Fig. 5. The difference of heat transfer coefficients among bundles due to the steep radial power distribution can be estimated well by the relation between the heat transfer coefficient and the normalized pressure drop obtained in the flat radial power distribution test. It is therefore concluded that the radial distribution of normalized pressure drop is the dominant factor of the radial distribution of heat transfer coefficient above the quench front.

4. Heat Transfer Analysis

4.1 Heat Transfer model

In order to clarify the physical mechanism of the heat transfer enhancement for high power bundles, the experimentally derived heat transfer coefficient should be quantitatively predicted with a heat transfer correlation based on the local hydraulic conditions. The flow pattern above the quench front during the reflood phase is classified into three regimes; inverted annular flow, dispersed flow, and transition flow between these two flow regimes. Several heat transfer correlations have been proposed for each of these flow regimes. Here, a film boiling heat transfer correlation and a dispersed flow heat transfer correlation are evaluated for the SCTF,

the CCTF and the FLECHT-SEASET data.

(1) Bromley type film boiling correlation

The Bromley type correlations are widely used for the inverted annular and transition flow regimes. For example, Murao and Sugimoto proposed the following film boiling heat transfer correlation⁽³⁾:

$$h = 0.94 \left[\frac{k_g^3 \rho_g \rho_l H_{fg} g}{L_q \mu_g \Delta T_{sat}} \right]^{\frac{1}{4}} (1 - \alpha)^{\frac{1}{4}} + \epsilon \sigma_{SB} (1 - \alpha)^{\frac{1}{2}} (T_w^4 - T_{sat}^4) / \Delta T_{sat} \quad (1)$$

Eq.(1) could predict the heat transfer coefficients obtained from the small scale reflood experiments and some FLECHT experiments.⁽³⁾ Since these experiments were performed with relatively small cores, the fluid behavior is considered to be approximately one-dimensional. In the case of a large core, however, the Bromley type correlations are not expected to be able to predict the two-dimensional heat transfer behavior of the transition flow regime because the effects of the local flow conditions are weak in these correlations. On the other hand, a heat transfer model for a dispersed flow is expected to explain the two-dimensional heat transfer behavior because the effects of steam velocity and liquid fraction are more strongly accounted for in this model.

(2) Dispersed flow model

Based on the dispersed flow film boiling heat transfer model, the total heat transfer coefficient is given by a sum of the following three terms:

$$h = h_r + h_{wd} + h_{fc} \quad (2)$$

where, h_r , h_{wd} and h_{fc} are heat transfer coefficients due to the wall to droplets radiation, droplet impingement against the wall and forced convection of steam, respectively. Here, the heat transfer coefficients are defined at the temperature difference between the rod surface and the saturated temperature.

The radiation heat transfer coefficient is calculated with the following equation:

$$h_r = \left[1 - \exp \left\{ -K_r \frac{3 D_h}{2 \delta} (1 - \alpha) \right\} \right] \epsilon \sigma_{SB} (T_w^4 - T_{sat}^4) / \Delta T_{sat} \quad (3)$$

where the ϵ and K_r were assumed to be 0.7 and 0.5, respectively, in the present calculation.

The heat transfer coefficient due to droplet impingement on the heated wall is evaluated using the Forslund-Rohsenow equation⁽⁴⁾:

$$h_{wd} = 0.2 \frac{\pi}{4} \left(\frac{6}{\pi} \right)^{\frac{2}{3}} (1 - \alpha)^{\frac{2}{3}} \left[\frac{k_g^3 H_{fg}^* g \rho_g \rho_l}{\Delta T_{sat} \mu_g \left(\frac{\pi}{6} \right)^{\frac{1}{3}} \delta} \right]^{\frac{1}{4}} \quad (4)$$

$$\text{where } H_{fg}^* = H_{fg} \left[1 + \frac{7}{20} \frac{C_{pg} \Delta T_{sat}}{H_{fg}} \right]^{-3}$$

The Dittus-Boelter correlation is used to evaluate the heat flux to steam due to forced convection. Since the heat transfer coefficients in the present calculation are defined at the temperature difference between the wall temperature and the saturated temperature, the forced convection heat transfer coefficient is expressed as:

$$h_{fc} = 0.023 Re_g^{0.8} Pr_g^{0.4} \frac{k_g (T_w - T_g)}{D_h \Delta T_{sat}} \quad (5)$$

The liquid fraction, $1 - \alpha$, the superheated steam temperature, T_g , and the droplet diameter, δ , are important factors for the calculation of heat transfer coefficients using Eqs.(3), (4) and (5).

The liquid fraction was obtained from the measured vertical differential pressures by correcting for the effects of frictional and accelerational pressure drops.

The superheated steam temperature was determined from the difference between the heat transfer from the wall to the steam and the interfacial heat transfer from the steam to the droplets.

The Sauter mean diameter is considered to be suitable for the representative droplet diameter in the heat transfer calculation, because both the void fraction and the interfacial area are preserved. In the present calculation, the Sauter mean diameter of droplets during the reflood phase was assumed to be 1.73 mm based on the flow observation results using the small scale reflood test facility⁽⁵⁾

4.2 Comparison with SCTF, CCTF and FLECHT-SEASET Flat Power Distribution Tests

In Fig. 6, the heat transfer coefficients calculated with the Murao-Sugimoto correlation (Eq.(1)) and the present method (Eq.(2)) are compared with the data from the flat power distribution test (S2-14). The calculated result with Eq.(2) agrees well with the experimental result except during the short time before quenching. On the other hand, Eq.(1) underestimates the experimental result.

Figure 7 shows the comparison of heat transfer coefficients calculated with Eqs.(1) and (2) with the experimental result for the CCTF flat power distribution test C2-06⁽⁶⁾. The calculated heat transfer coefficient with Eq.(2) approximately agrees with the CCTF result while the calculation tends to overestimate at the earlier period and underestimate at the later period. Eq.(1) can also predict well the CCTF result especially just before quenching.

Figure 8 shows the comparison of measured and calculated heat transfer coefficients for FLECHT-SEASET Run 31203⁽⁷⁾. In these calculations, the

measured steam temperatures were used instead of the calculated ones. The calculated results with Eq.(2) are fairly in good agreement with the FLECHT-SEASET data.

4.3 Applicable Range of the Dispersed Flow Model

The present dispersed flow correlation (Eq.(2)) was not able to predict the heat transfer coefficient at the later period before quenching because the flow pattern just above the quench front is the inverted annular flow instead of the dispersed flow. Since the liquid fraction during this later period was greater than 0.1, it is suggested that the present dispersed flow heat transfer model can be used when the liquid fraction is less than 0.1.

4.4 Effect of Superheated Steam Temperature

It is noted from Figs. 6 and 7 that Eq.(2) tends to overestimate the heat transfer coefficient during the early period of the transient. This is mainly attributed to the underestimation of the superheated steam temperature in the present calculation as shown in Fig. 9. It is shown in Fig. 10 that the calculated heat transfer coefficient with Eq.(2) using the measured steam temperature agrees well with the SCTF result for the early period before the quench of the thermocouple itself at about 130 s, while the calculated heat transfer coefficient using the calculated steam temperature is much higher than the test result during this period. The reason of the underestimation of steam temperature in the present calculation is considered to be the overestimation of the heat transfer rate from superheated steam to droplets.

4.5 Comparison with SCTF Steep Power Distribution Test

In the calculations of heat transfer coefficients with the dispersed flow model (Eq.(2)) for the steep radial power distribution test (Test S2-12), the steam cross flow across the rod bundles was neglected in the estimation of the steam up-flow rate. Therefore, steam velocity in the high power bundle tends to be overestimated and that in the low power bundle be underestimated in the present analysis.

Figure 11 shows the comparisons of the heat transfer coefficients calculated with Eqs.(1) and (2) with the experimental results at elevation of 2.33 m in Bundles 4 and 8 for Test S2-12. The heat transfer coefficient for the high power Bundle 4 is fairly well predicted with the present method (Eq.(2)) except during 30 to 100 s and after 200 s. The discrepancy during 30 to 100 s is due to the underestimation of the superheated steam temperature as mentioned before. The discrepancy after 200 s is due to the transition of flow pattern just before quenching. The calculation with Eq.(2) exhibits reasonably the large difference of heat transfer coefficients between Bundles 4 and 8 as observed in this test. However, the Murakami Sugimoto correlation (Eq.(1)) cannot predict the large difference in the heat transfer coefficients between bundles.

4.6 Mechanism of Heat Transfer Enhancement in High Power Bundle

The heat transfer coefficients due to radiation, droplet impingement and forced convection of steam calculated with Eqs.(3),(4) and (5), respectively, for the high and low power bundles in Test S2-12 are shown in Fig. 12. The forced convection term is dominant among these three heat transfer terms especially during the initial period.

Shown in Figs. 13 and 14 are the steam velocities and the liquid fractions, respectively, which were used in the present calculation. It is indicated from these figures that the higher power bundle gives not only the higher steam velocity but also the higher liquid fraction after about 60 s. Before 60 s, the effects of frictional and accelerational pressure drops were possibly over-corrected in the high power bundle.

The forced convection heat transfer coefficient is always higher in the high power bundle. Since the liquid fraction is higher in the high power bundle, the decreasing of superheated steam temperature due to the heat transfer between steam and droplets is considered to be larger in the high power bundle than the low power bundle. Therefore, the higher forced convection heat transfer coefficient in the high power bundle is caused by not only the higher steam velocity but also the higher liquid fraction in this bundle.

The heat transfer coefficients due to radiation and droplet impingement are also higher in the high power bundle except during the initial 40 or 60 s. The reasons are considered as follows: 1) The higher liquid fraction results in the higher droplet impingement heat transfer by increasing the number of droplets dispersed around the heated wall. and 2) In addition to the higher rod temperature, the higher liquid fraction gives a higher radiation absorption rate to the droplets.

The higher steam velocity and the higher liquid fraction in the high power bundle give the higher frictional and gravitational pressure drops, respectively, resulting in the higher vertical pressure drop in this bundle. Therefore, the above-mentioned mechanism of the two-dimensional heat transfer behavior in the steep radial power distribution test is consistent with the close correlation between the two-dimensional distribution of heat transfer coefficient and that of normalized pressure drop.

5. Conclusion

- (1) The normalized vertical pressure drop above the quench front was higher for the high power bundles and lower for the low power bundles under nonuniform radial power distribution.
- (2) The heat transfer enhancement for the high power bundles and degradation for the low power bundles were strongly related to the two-dimensional distribution of normalized vertical pressure drop.
- (3) The Bromley type film boiling heat transfer correlation was not able to predict the large difference of heat transfer coefficients between bundles.
- (4) Based on a dispersed flow model, the heat transfer coefficients for SCTF, CCTF and FLECH-SEASET flat power distribution tests were fairly well predicted with the sum of three terms; radiation, droplet impingement, and forced convection of steam, when the liquid fraction was less than 0.1.
- (5) The heat transfer enhancement for the high power bundles due to the radial power distribution was predicted with the present dispersed flow model.

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References

- (1) Y. Murao, et al., 12th WRSR Information Meeting, (1984).
- (2) T. Iwamura, et al., 13th WRSR Information Meeting, (1985).
- (3) Y. Murao and J. Sugimoto, J. Nucl. Sci. Technol., 18[4], 275 (1981).
- (4) R.P. Forslund and W.M. Rohsenow, J. Heat Transfer, Nov. 399 (1968).
- (5) J. Sugimoto and Y. Murao, JAERI-M 84-131 (1984).
- (6) H. Akimoto et al., J. Nucl. Sci. Technol., 22[7], 538 (1985).
- (7) M.J. Loftus et al., NUREG/CR-1532 (1980).

Nomenclature

C_p	: Specific heat	(J/kg·K)
D_h	: Hydraulic diameter	(m)
g	: Acceleration due to gravity	(m/s ²)
H_{fg}	: Latent heat of evaporation	(J/kg)
h	: Heat transfer coefficient	(W/m ² ·K)
K_r	: Constant in eq. (3)	
k	: Thermal conductivity	(W/m·K)
L_q	: Distance from quench front	(m)
Pr	: Prandtl number	
Re	: Reynolds number	
T	: Temperature	(K)
ΔT_{sat}	: $T_w - T_{sat}$, wall superheat	(K)
α	: Void fraction	
δ	: Droplet diameter	(m)
ϵ	: Emissivity	
μ	: Dynamic viscosity	(Pa·S)
ρ	: Density	(kg/m ³)
σ_{SB}	: Stefan Boltzman constant	(W/m ² ·K ⁴)

[Subscripts]

fc	: Forced convection	r	: Radiation
g	: Gas phase	sat	: Saturation
l	: Liquid phase	w	: Wall
		wd	: Wall to droplet

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Table 1 Test conditions for SCTF Tests S2-12 and S2-14

Test No.	S2-12	S2-14
Radial power distribution	Steep*	flat
System pressure	0.2 MPa	
Accumulator flow rate	26 kg/s(8.5 cm/s)	
Accumulator injection period	36 s	
LPCI flowrate	4.7 kg/s(1.6 cm/s)	

* Normalized power ratio: 1.0(Bundles 1 & 2), 1.2(Bundles 3 & 4)
1.0(Bundles 5 & 6), 0.8(Bundles 7 & 8)

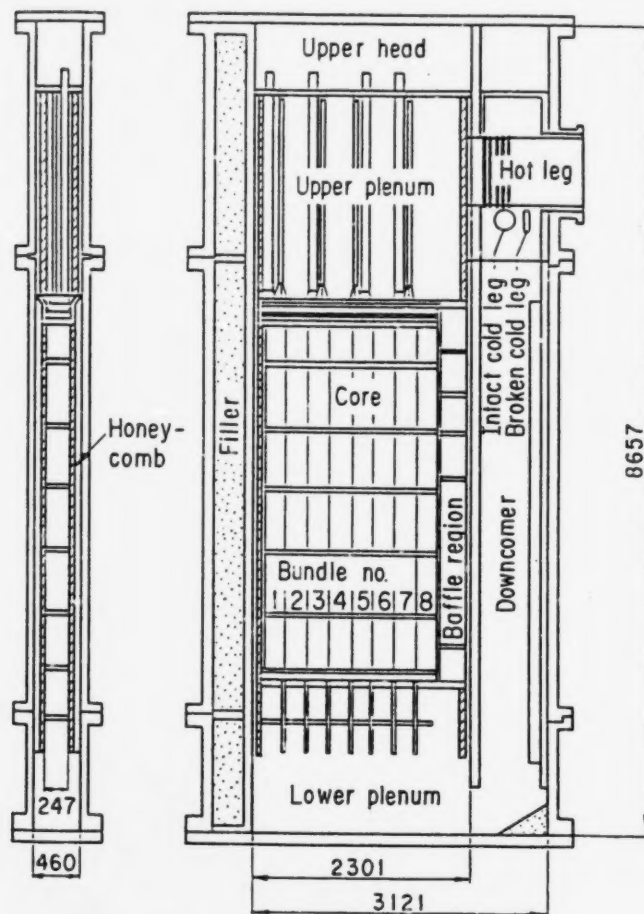


Fig. 1 Cross section of SCTF pressure vessel

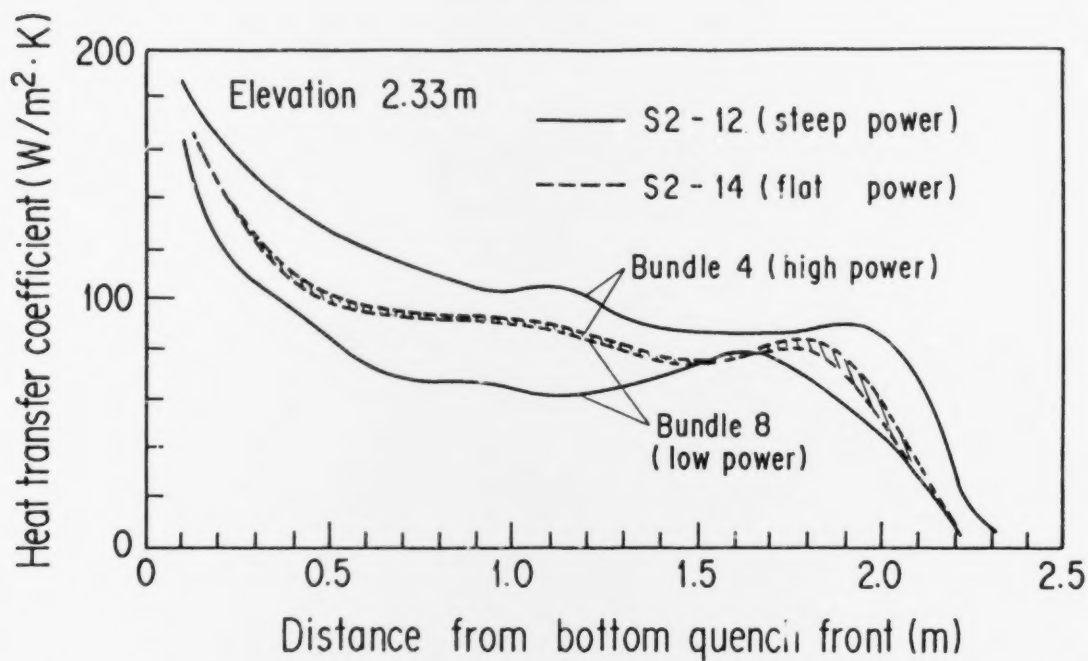


Fig. 2 Effect of radial power distribution on heat transfer coefficient

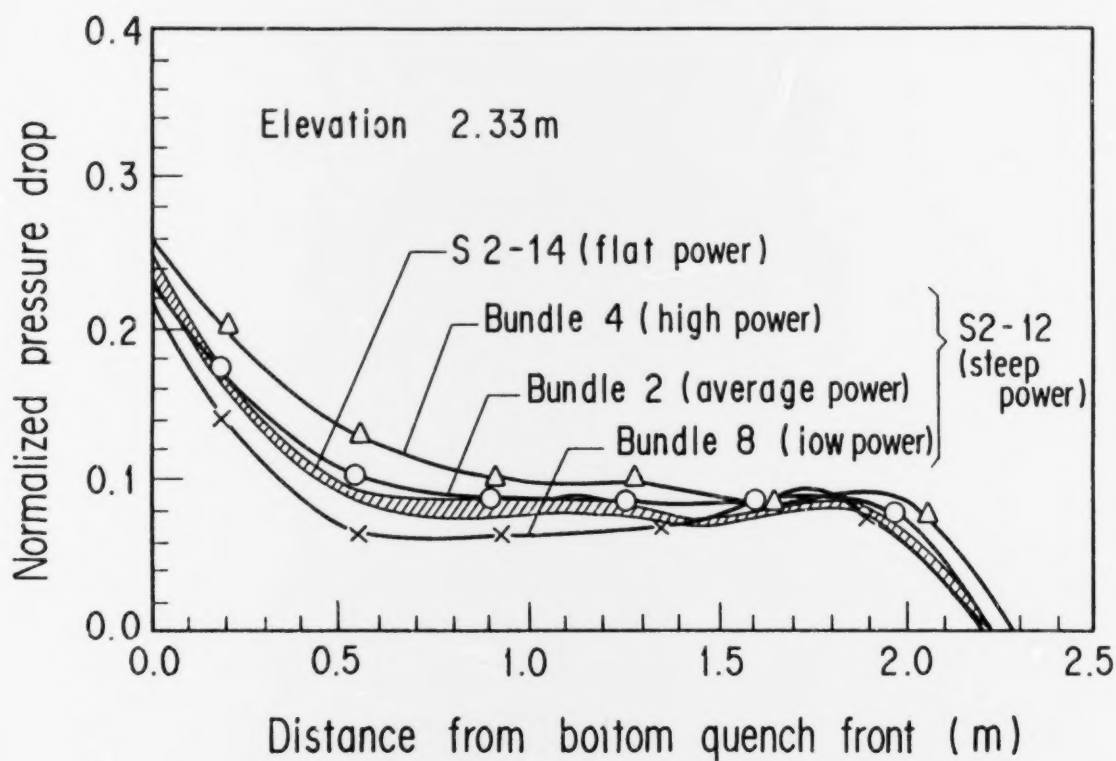


Fig. 3 Effect of radial power distribution on normalized pressure drop

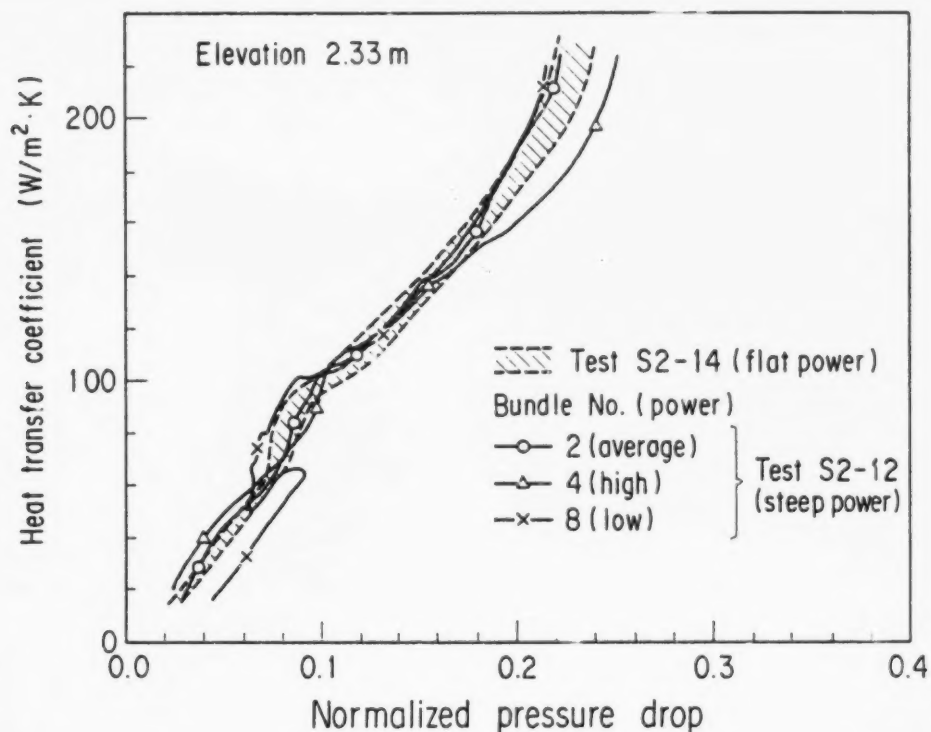


Fig. 4 Relation between heat transfer coefficient and normalized pressure drop

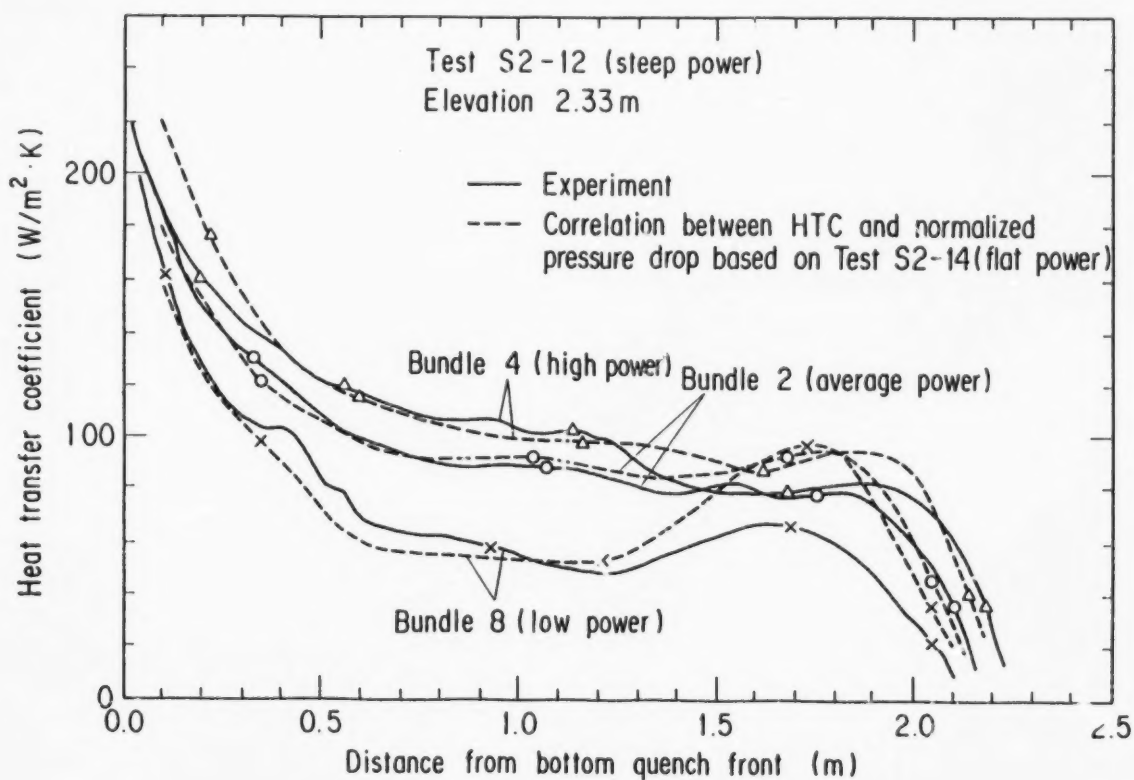


Fig. 5 Comparison of measured heat transfer coefficients with calculated results using an empirical relation between heat transfer coefficient and normalized pressure drop

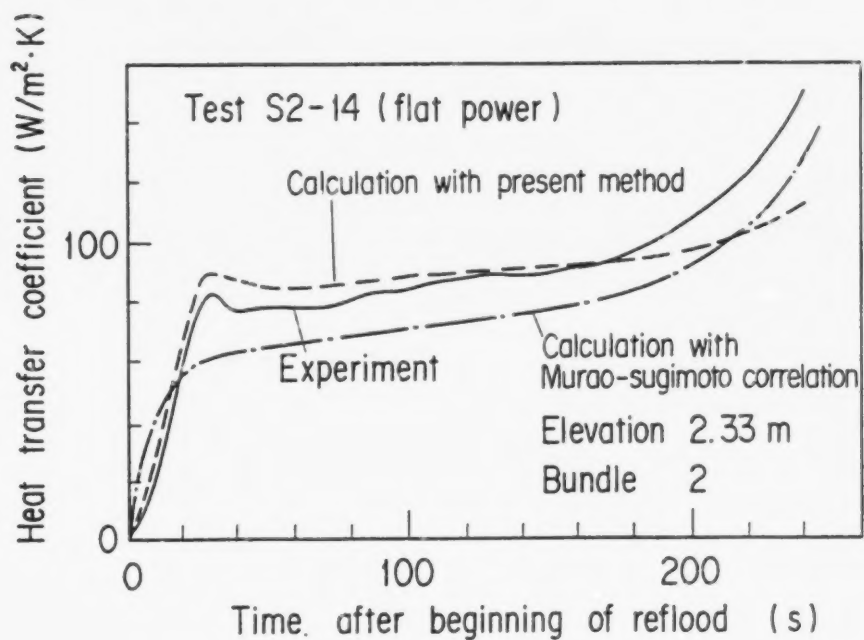


Fig. 6 Comparison of calculated heat transfer coefficients with SCTF test result

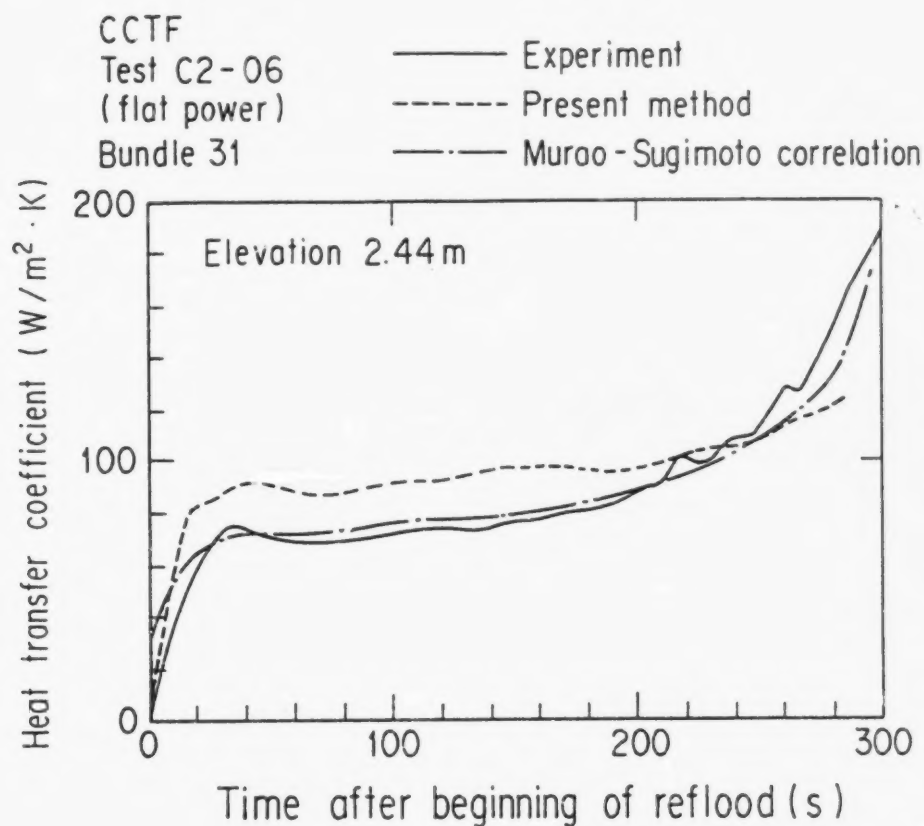


Fig. 7 Comparison of calculated heat transfer coefficients with CCTF test result

FLECHT SEASET Run 31203

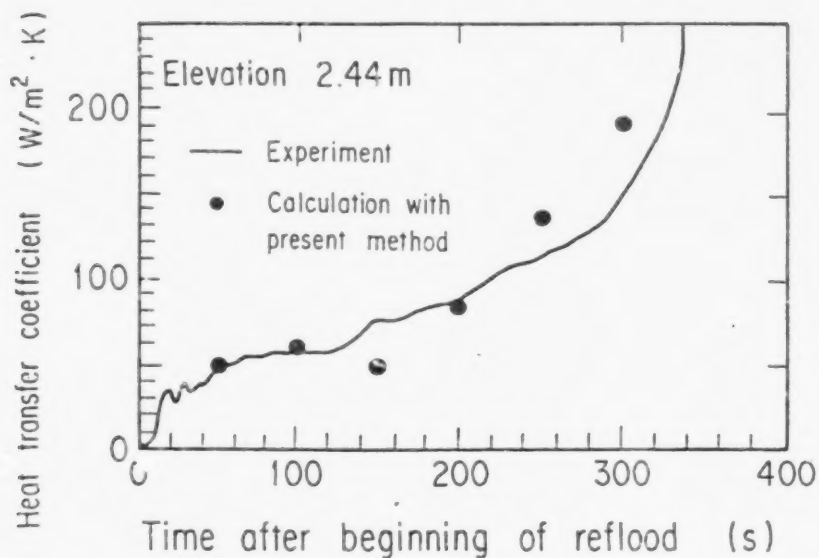


Fig. 8 Comparison of calculated heat transfer coefficients with FLECHT-SEASET test result

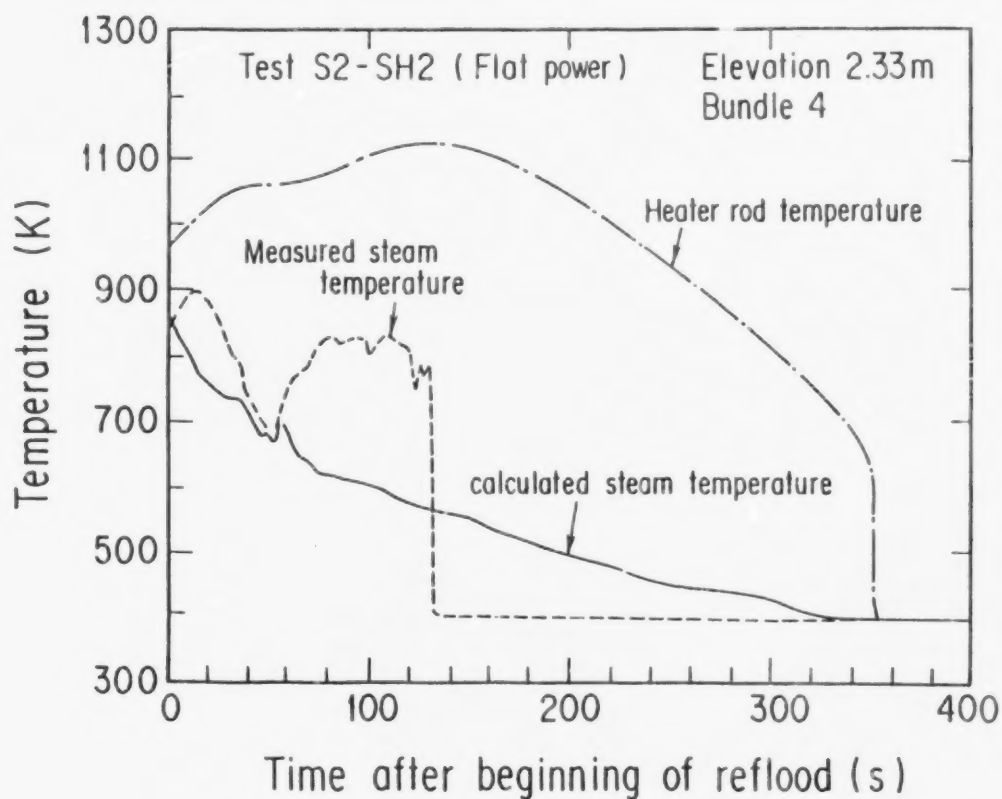


Fig. 9 Comparison of measured and calculated steam temperatures

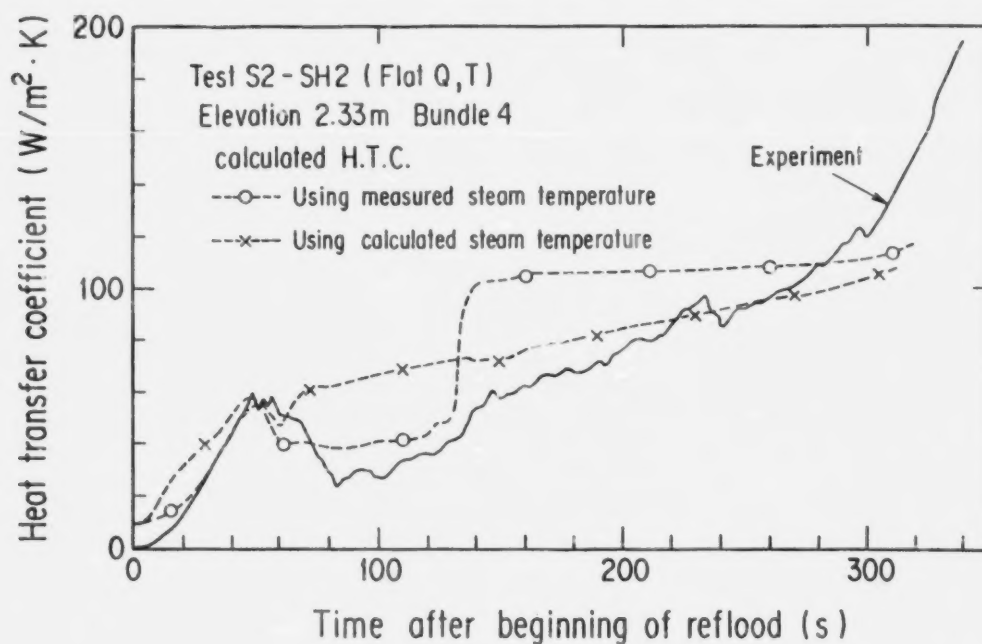


Fig.10 Comparison of calculated heat transfer coefficients using measured and calculated steam temperatures with SCTF test result

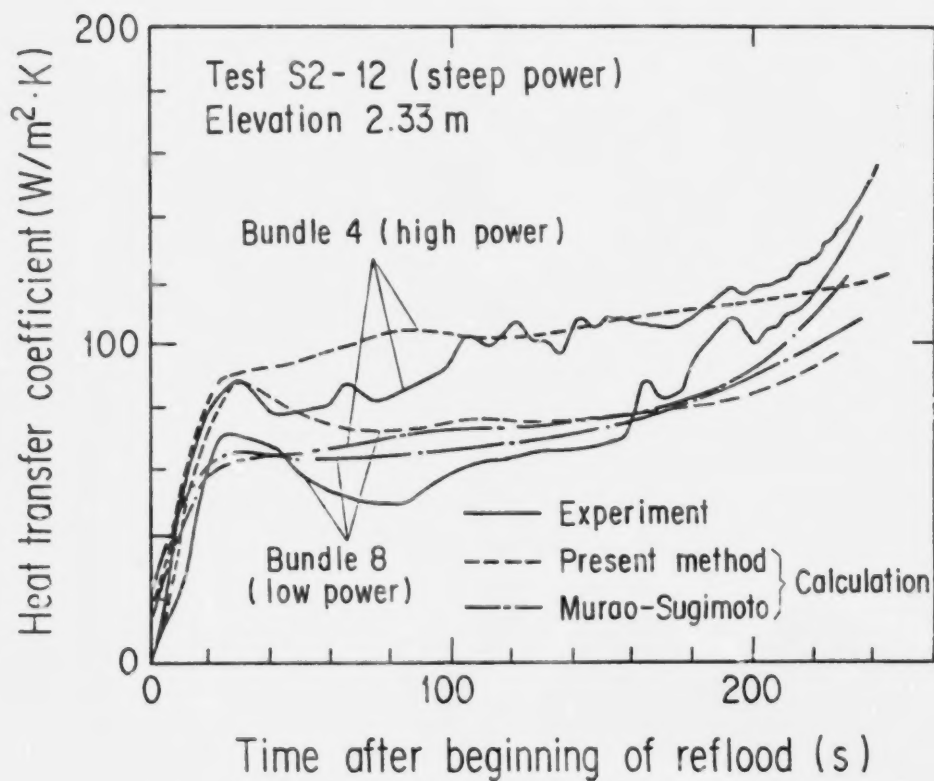


Fig.11 Prediction of two-dimensional heat transfer behavior with dispersed flow model and Murao-Sugimoto correlation

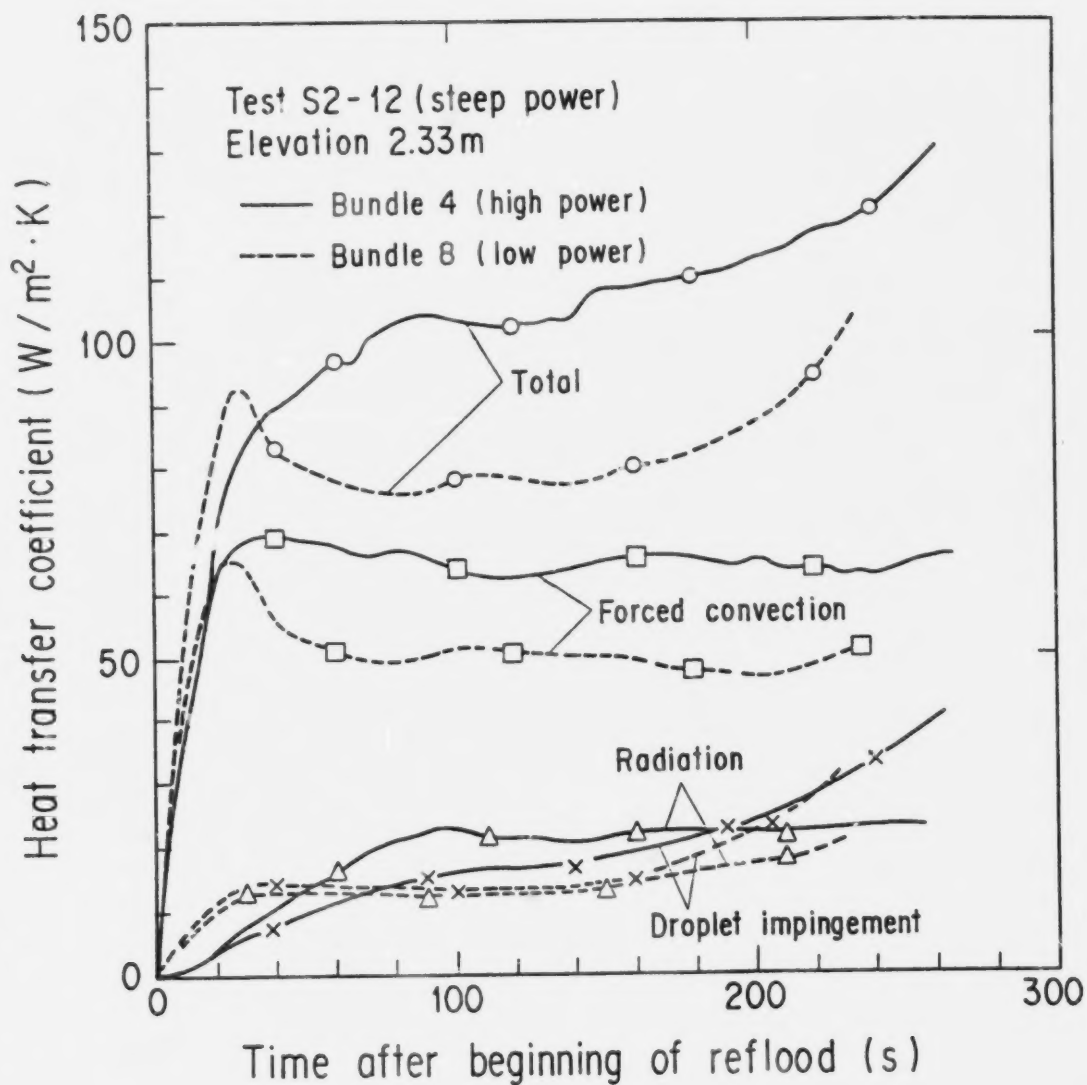


Fig.12 Calculated heat transfer coefficients in high and low power bundles due to forced convection, radiation, and droplet impingement

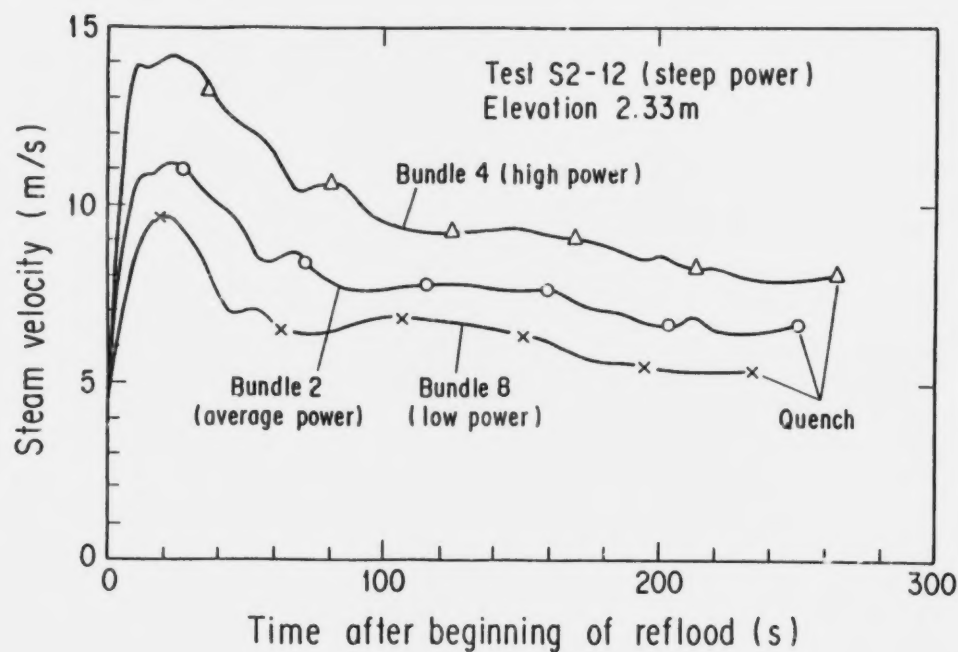


Fig.13 Two-dimensional distribution of steam velocities used in heat transfer calculation with dispersed flow model

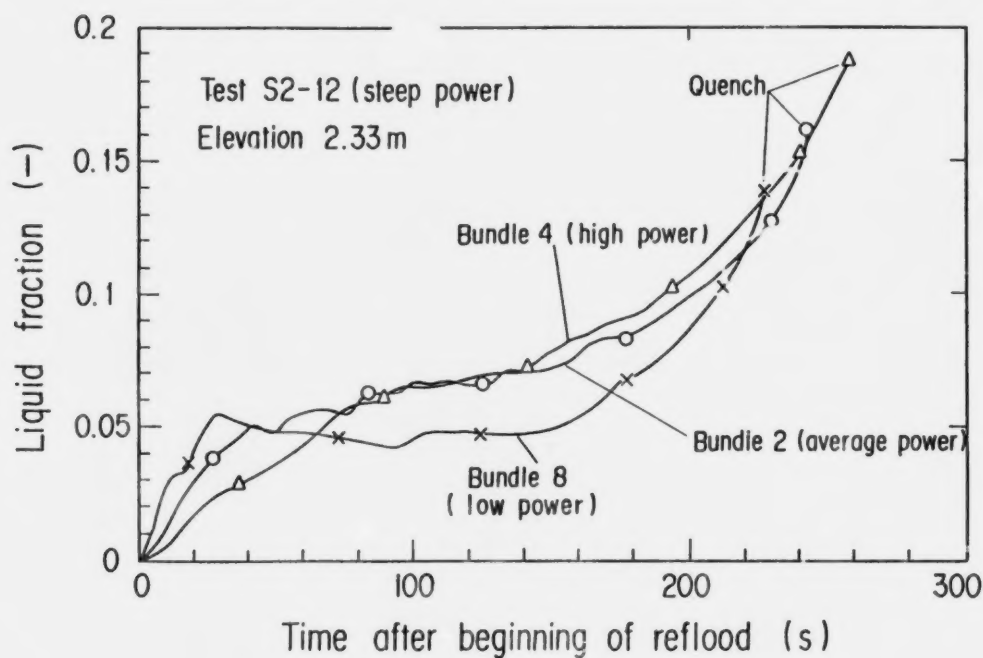


Fig.14 Two-dimensional distribution of liquid fractions used in heat transfer calculation with dispersed flow model

DEVELOPMENT OF REFLOOD MODEL AT JAERI

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1. Introduction

In the safety analysis on the Loss-of-Coolant Accident (LOCA) of pressurized light water reactors (PWRs), it is very important to evaluate the temperature history of the fuel rod claddings during the reflood phase, which is governing the integrity of the first enclosure of the fission product. As tools for realistic evaluation of the fuel claddings, we are developing best estimate models for core cooling during reflood phase based on the physical understanding of the phenomena. In order to investigate the reflooding phenomena, data from Small scale reflood test facility, Cylindrical Core Test Facility (CCTF), Slab Core Test Facility (SCTF) and Upper Plenum Test Facility (UPTF) in FRG has been used.

2. Model development for PWRs with a cold leg injection type ECCS

For PWRs with a cold leg injection type ECCS, a reflood analysis code REFLA has been developed. The previous version of REFLA, REFLA/MOD 3⁽¹⁾, successively predicted temperature transients of an average power rod in a CCTF test as shown Fig. 1⁽²⁾. Figure 2 shows the good comparison of the quench times and turnaround temperatures for CCTF tests with various test conditions.

The latest version of the code is REFLA/MOD4. This version includes some new improvements in the core thermo-hydrodynamic models, that is (1) local power effect model for evaluating radial power

The work was performed under contract with the Atomic Energy Bureau of Science and Technology Agency of Japan.

distribution effect, (2) pellet-cladding gap conductance effect, (3) cladding material effect on quench propagation. This code successfully shows the capability of realistic simulation of the CCTF tests and the SEFLEX test.

Figure 3 shows the good predictability of the local power effect model for peak power rods. The results of simulation of the SEFLEX tests⁽³⁾ are indicated in Fig. 4⁽⁴⁾. In Type 1 there is no gap between Inconel clad and inner material and in Type 2 there is gap filled with helium between Zircaloy clad and inner material.

Grid spacer effect and flow housing effect are being incorporated into REFLA/MOD5. And the mixing vane effect and mechanisms of core water accumulation are studied in small scale reflood tests. Figure 5 shows the results of simulation calculation with grid spacer effect model⁽⁵⁾ for JAERI small scale reflood test. The figure indicates that this model is reasonable.

The SCTF and CCTF test results showed the core cooling enhancement due to the radial power effect. The effect on the peak clad temperature, however, seems to be weak, since the turn-around of clad temperature occurs in a short time after reflood initiation. In lower flooding rate cases which expect to cause delay of turn-around, the effect on the peak clad temperature becomes more important. Therefore qualitative explanation on the core cooling enhancement are being investigated as shown in the previous presentation.

The system model of the REFLA code includes the pressurization model of the pressure vessel due to the pressure loss in the broken cold leg nozzle, and the water accumulation model in the upper plenum, the downcomer bypass model and the pressure loss model in the broken cold leg nozzle.

The core model of the REFLA code was installed in the TOODEE 2 code in the WREM code system. The improved WREM code provides more flexibility, since the original WREM code uses empirical FLECHT heat transfer correlation, while the improved uses the physical model. Figure 6 shows the calculated clad surface temperatures for peak power rod in a PWR. The calculation were performed with original and improved WREM code⁽⁶⁾.

In order to examine the applicability of REFLA code to cores of different configuration, the simulation calculation was performed for NEPTUNE reflood tests with a tight lattice core ($p/d = 1.13$)⁽⁷⁾.

Figure 7 shows the comparison between measured and calculated quench front envelopes⁽⁸⁾. This figure indicates the qualitative agreements and necessities of further model improvement.

3. Model development for PWRs with a combined injection type ECCS

For PWRs with a combined injection type ECCS like German PWRs, modification of the REFLA code and the J-TRAC code are considered. In the modeling, the following items should be studied:

- (1) Condensation of steam in the hot legs, the upper plenum, the lower plenum, and the core,
- (2) Counter current flow at the end box of the core, and
- (3) Core cooling by fall-back water and bottom flooding water.

For these purpose, CCTF, SCTF and UPTF test results are being analysed. In the case of UPTF, Once the water fall-back occurs, the steam injected into core seems to be condensed by falling-back sucooled water in the core and upward steam flow at the end box is decreased, if there is no feed-back system for steam injection. While in the facility with simulated fuel rod the fall-back water feed the source of steam in the heated core and the response of the upward steam flow against fall-back flow expected to be different. SCTF is expected to provide the information on the feed back system for steam injection into the core of UPTF. Figure 8 shows the comparison of UPTF and SCTF. And US advanced instrumentation like drag body detectors and TRAC code are expected to assist to couple both facilities.

References

- (1) Murao, Y., et al: JAERI-M 84-243, (1984)
- (2) Okubo, T. and Murao, Y.: J. Nucl. Sci, Technol., 22[12], pp.983 ~ 994, (1985)
- (3) Ihle, P., et al.: Presented at 1st Int. Workshop on Fundamental Aspects of Post-dryout Heat Transfer at Salt Lake City, (1984)
- (4) Sugimoto, J., et al.: J. Nucl. Sci. Technol., 23[4], pp.315 ~ 325, (1986)
- (5) Sugimoto, J., et al.: Presented at 3rd Int. Conference on Reactor Thermal Hydraulics New Port, (1985)

- (6) Hojo, T., et al.: Presented at Fall Mtg. of the Atomic Energy of Society Japan, Oct., (1986)
- (7) Dreier, J., et al.: Presented at ENC' 86 "Nuclear Energy of Today and Tomorrow", June, (1986)
- (8) Hojo, T., et al.: To be published in JAERI-M Report.

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Development of Reflood Model at JAERI

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Presented at

14th Water Reactor Safety Information Meeting
NBS, Gaithersburg, Maryland
October 30, 1986

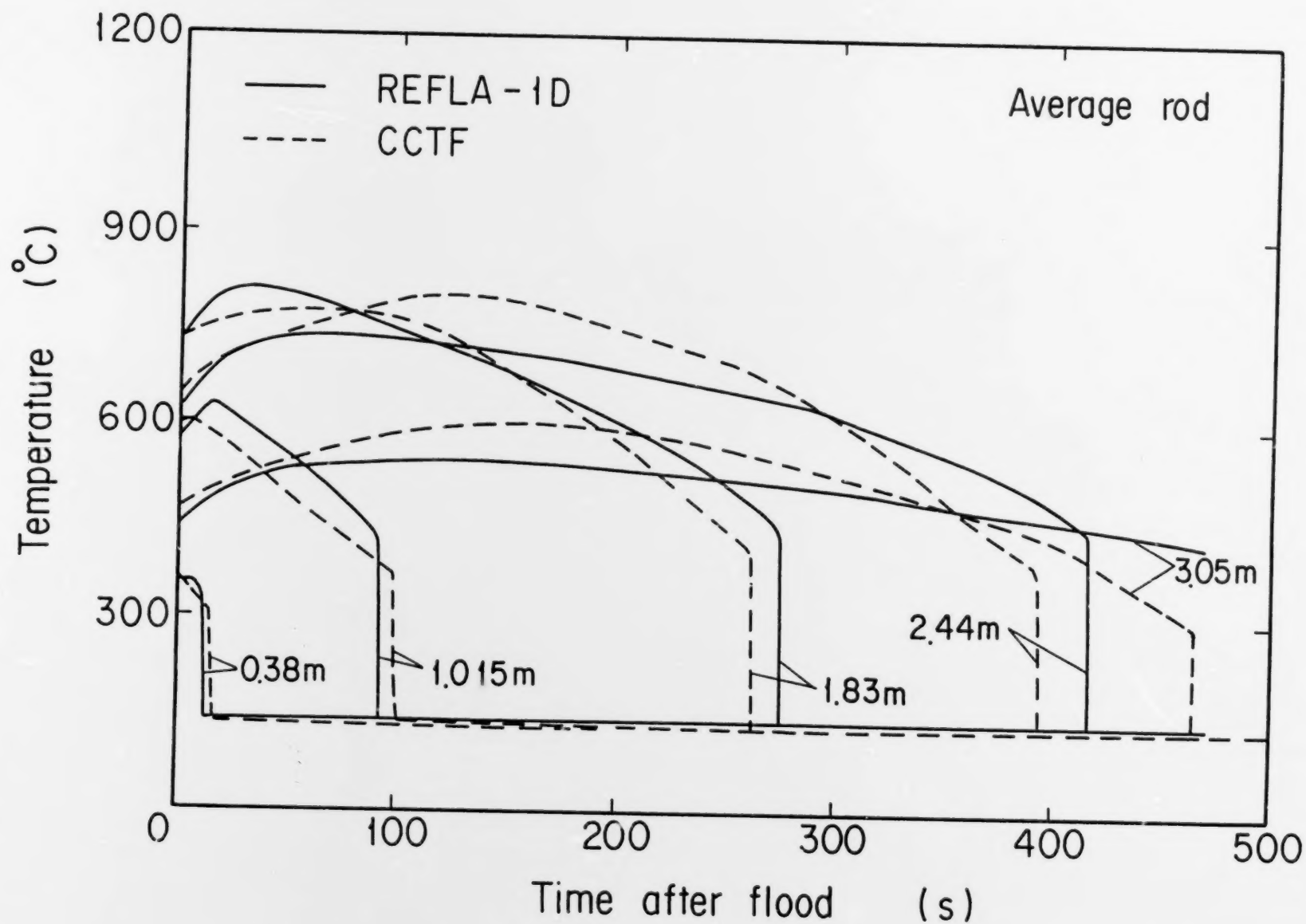


Fig.1 Measured and predicted temperature transients for a CCTF test

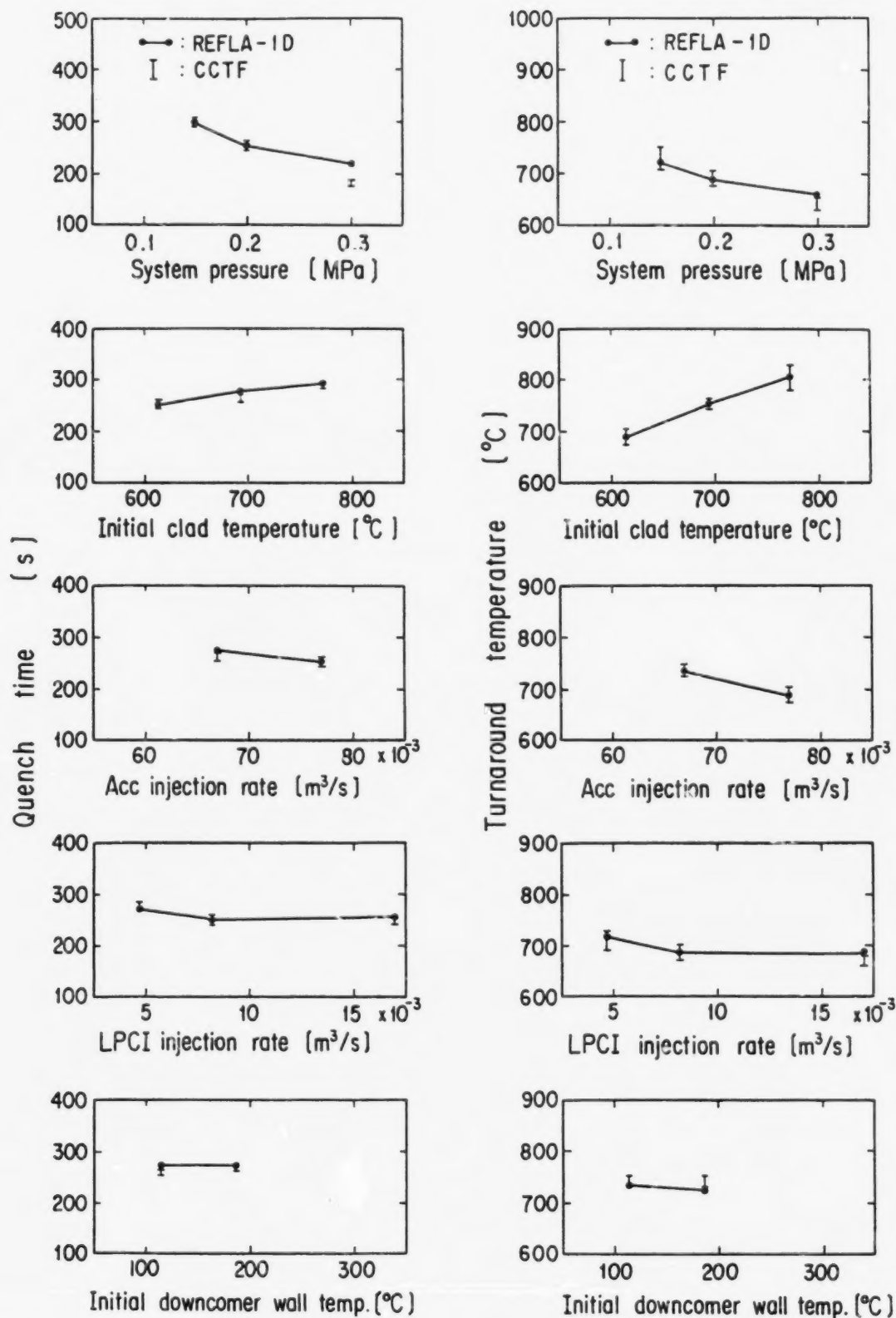


Fig.2 Comparison of quench times and turnaround temperatures between calculations and CCTF tests for several parameters

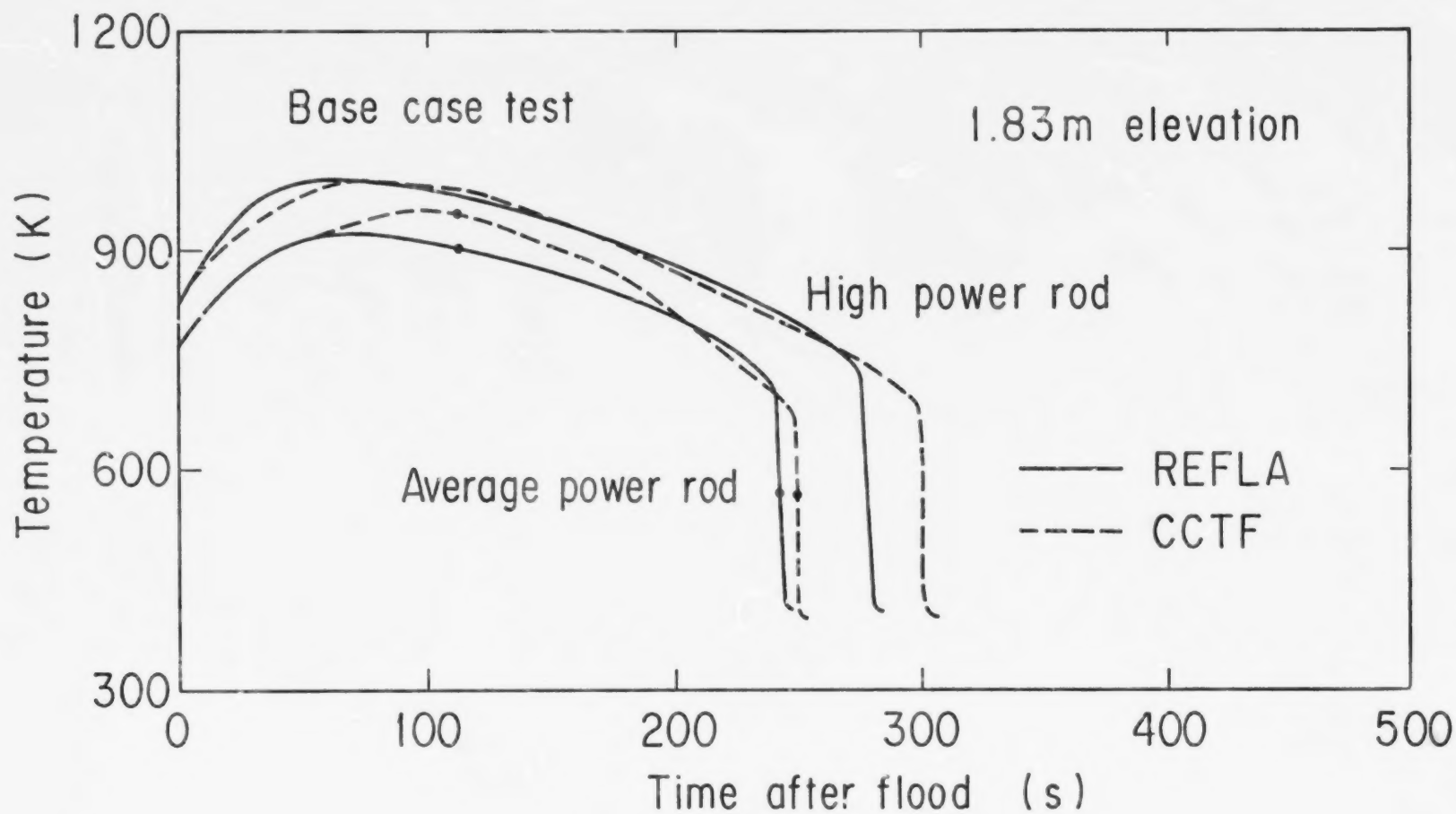


Fig.3 Comparison of clad temperature histories between REFLA prediction and CCTF data for local power effect

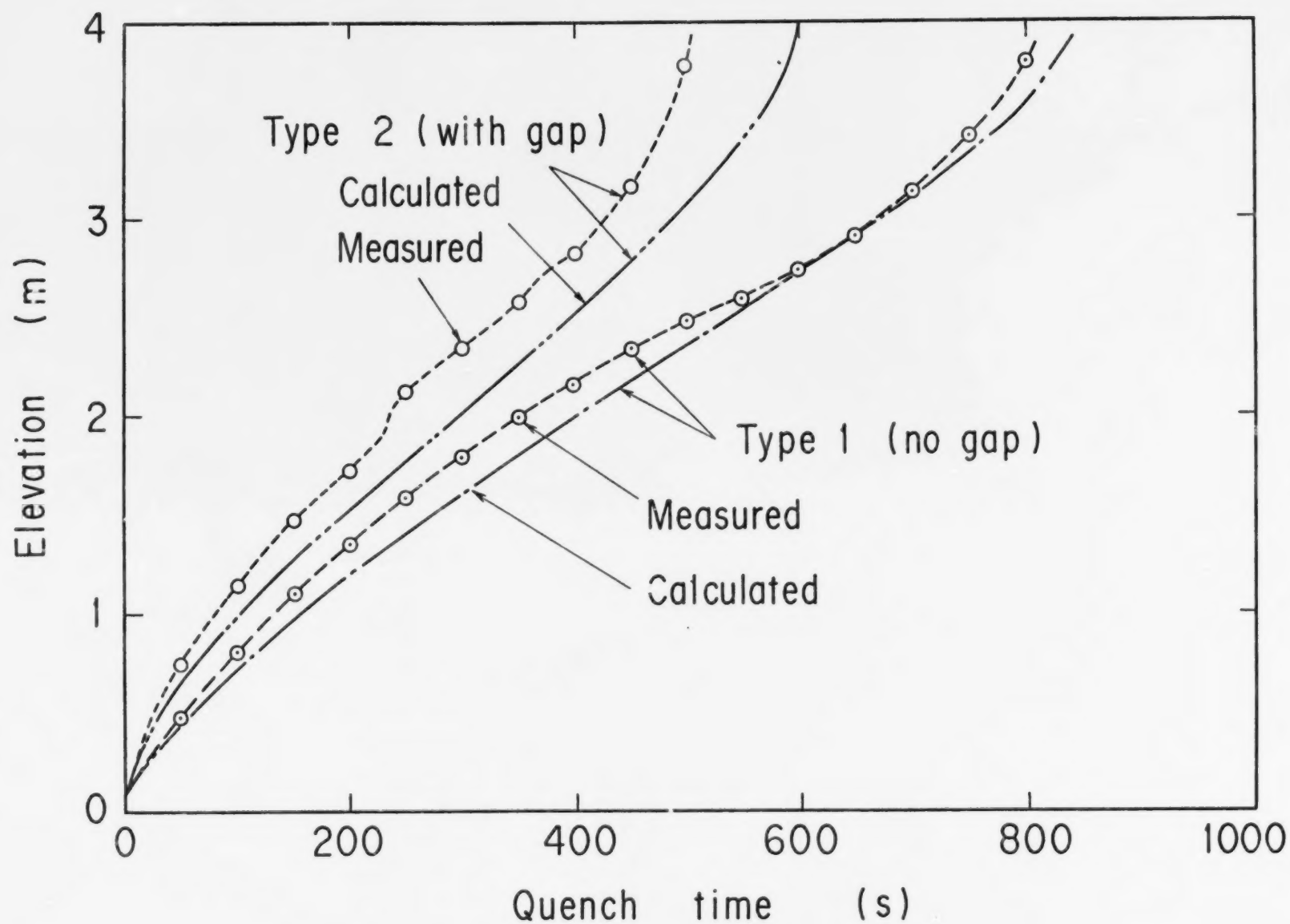


Fig.4 Calculated and measured quench front movements in SEFLEX test

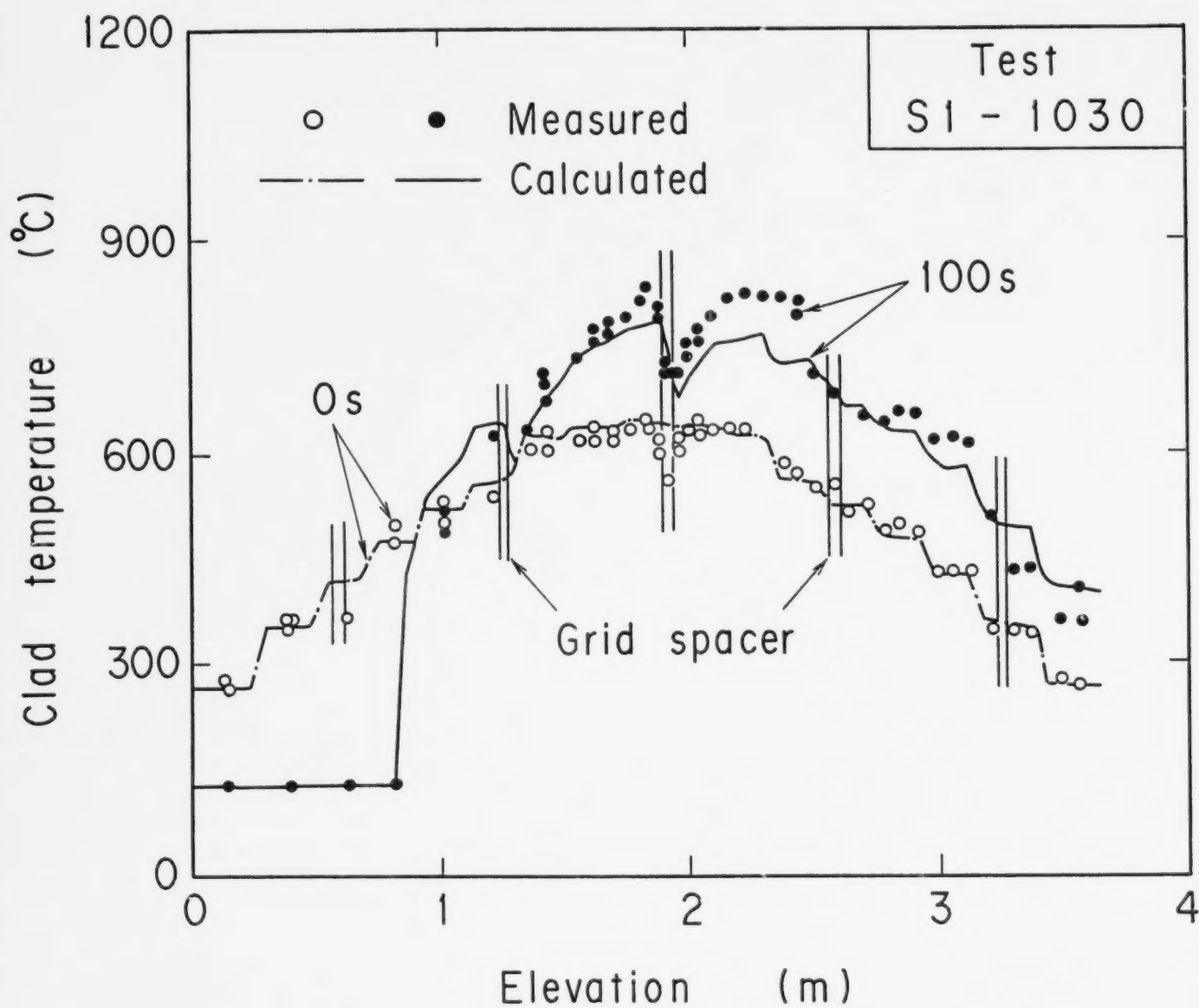


Fig.5 Simulation calculation of grid spacer effect with REFLA code

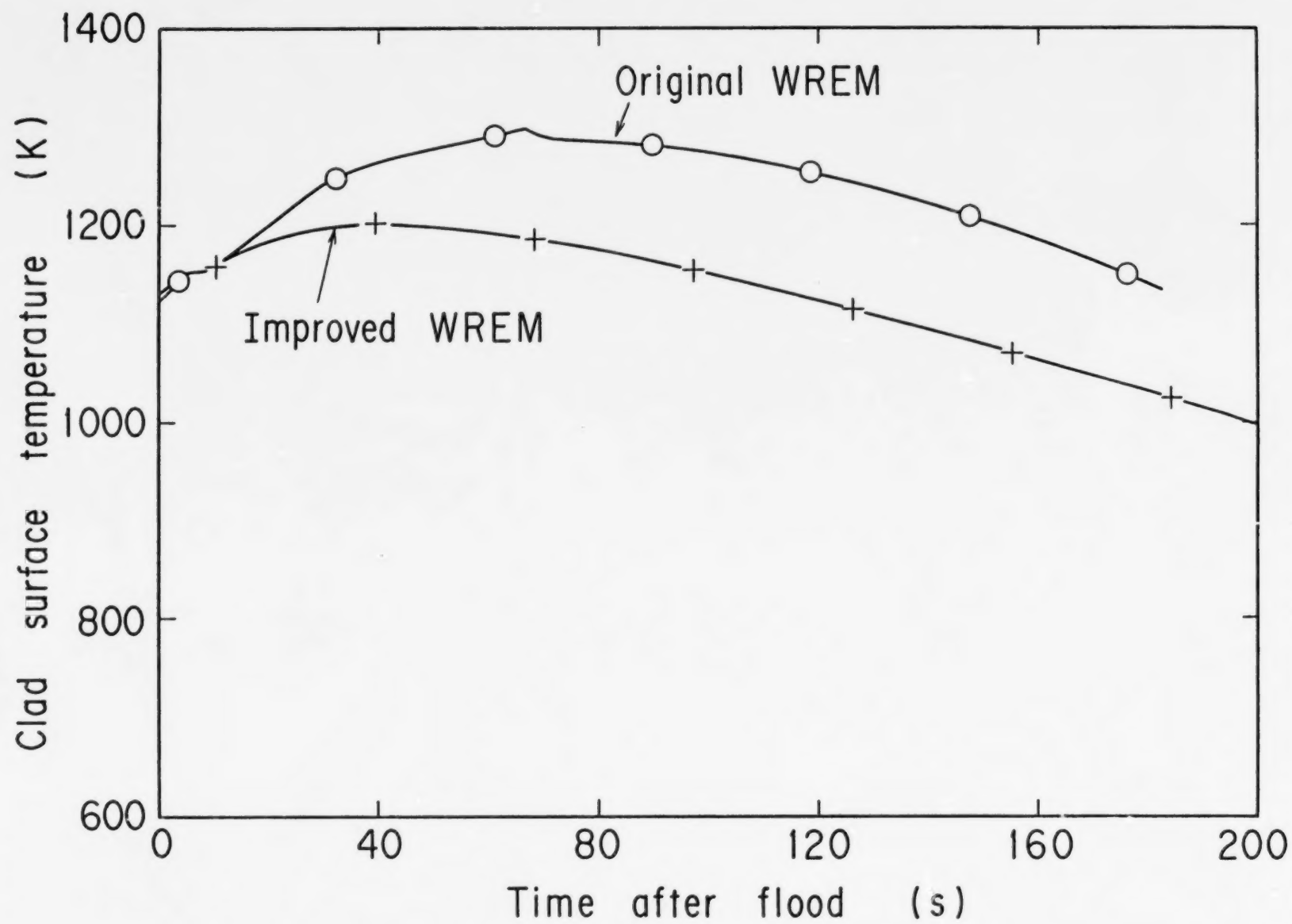


Fig.6 Calculational results for peak power rod of a PWR

955

Pressure : 4.1 bar
 Subcooling : 80K
 Flooding rate : 10 cm/s
 Power : 2.45 kw
 Initial temp : 1034 K

—— NEPTUN
 ---- REFLA

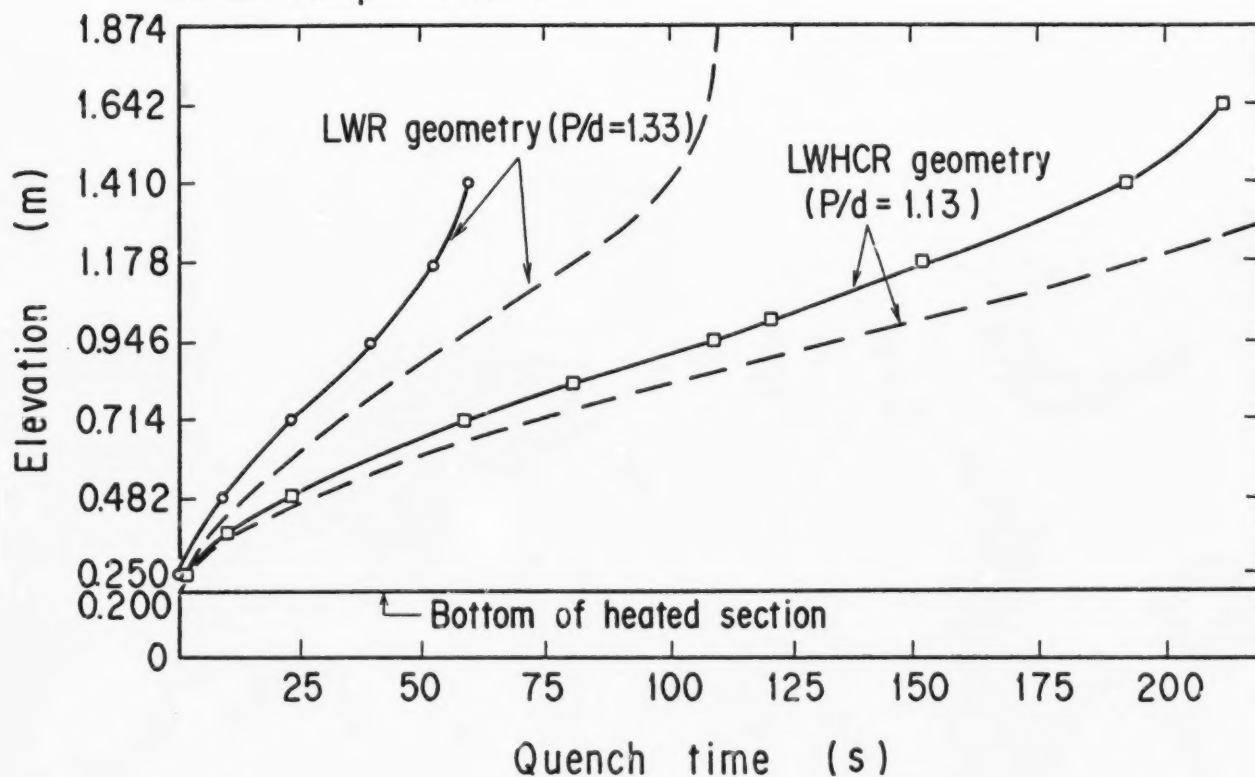


Fig.7 Simulation calculation of quench front movement in NEPTUNE tests with REFLA code

Items to be modeled for physical understanding of phenomena

- (1) Condensation (in hot leg, upper plenum, core etc.)
- (2) Multi-dimensional tie plate CCF with subcooled water
(prediction of fall-back flow rate)
- (3) Fluid mixing and water accumulation in upper plenum
- (4) Multi-dimensional core thermal-hydraulic behavior
- (5) Water accumulation in hot leg and steam generator header
- (6) Steam generation in primary side of steam generator

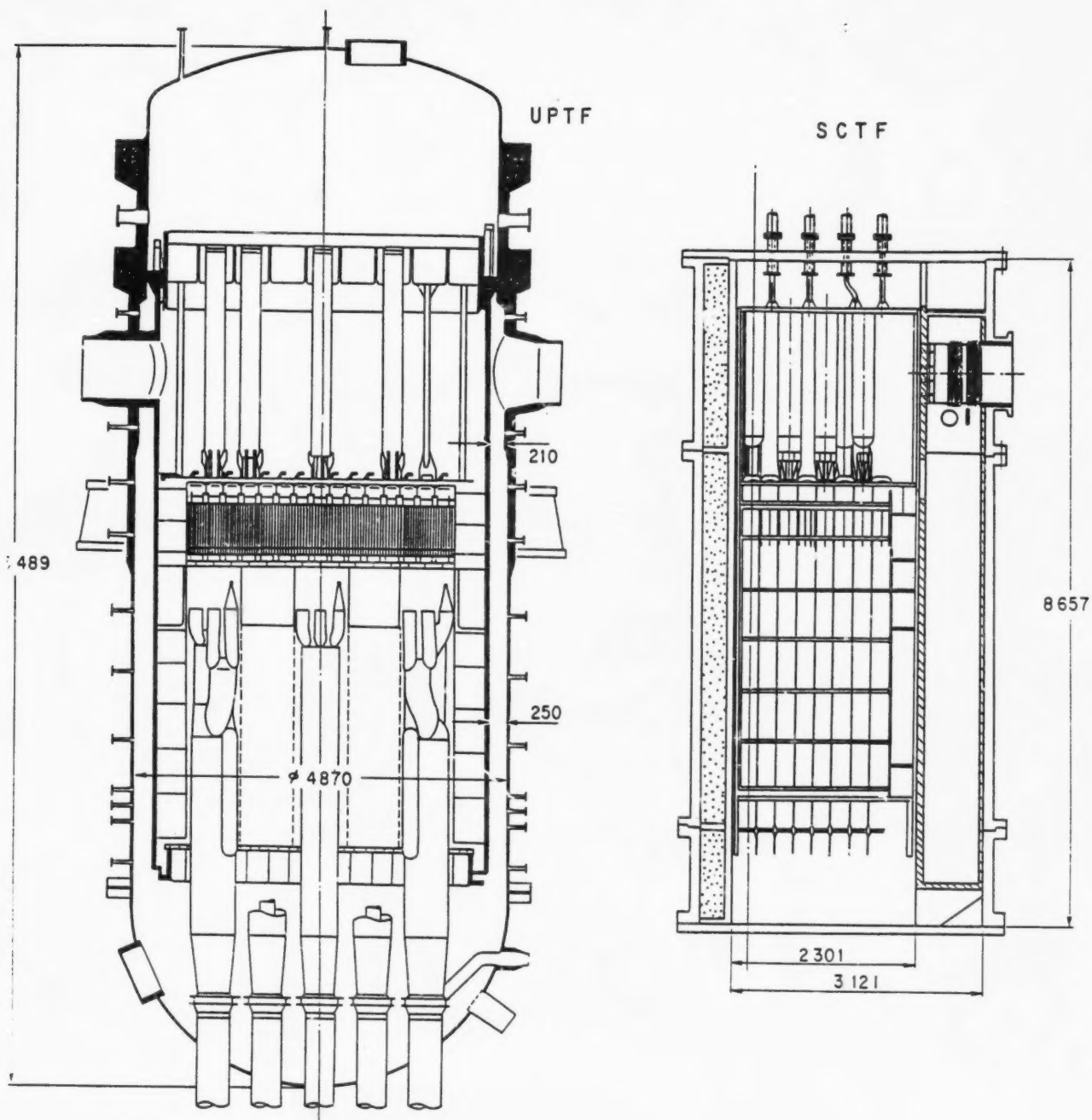


Fig.8 Test vessels of UPTF and SCTF

UPTF TEST RESULTS
FIRST 3 SEPARATE EFFECT TESTS

BY

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Abstract

The first separate effect tests were run in the Upper Plenum Test Facility - a 1:1 representation of a PWR primary system. These tests were focusing the simultaneous steam up - and water down flow phenomena at the upper tie plate, the fluid-fluid mixing in the cold leg and downcomer and the counter-current flow conditions of steam and saturated water in a PWR - hot leg.

1. INTRODUCTION

The Upper Plenum Test Facility (UPTF) Experimental Program, sponsored by the Ministry for Research and Technology (BMFT) is the German contribution to the trilateral 2D/3D Project, and is performed within international cooperation among Japan (JAERI), USA (USNRC) and the Federal Republic of Germany (BMFT).

The UPTF simulates the primary cooling system of a KWU 1300 MW PWR. The system configuration of the UPTF is shown in Fig. 1. Major dimensions of the UPTF are depicted in Fig. 2.

The upper plenum including internals, the downcomer and the four connected loops are represented in 1:1 scale. The core is simulated with controlled injection of steam and water supplied from external sources.

The three intact loops are equipped with flow restrictors to simulate the reactor coolant pumps, and with steam/water separators representing the steam generators. The hot and cold legs of the broken loop lead through steam/water separators and break valves into the containment simulator. Breaks of variable size can be simulated in the hot and in the cold leg respectively.

According to the internationally agreed test matrix, the following three separate effect tests have been performed during March to June 1986.

- Test No. 12: Simultaneous steam up- and water downflow at the tie plate
- Test No. 1: Fluid-fluid mixing in cold leg and downcomer
- Test No. 11: Countercurrent flow of steam and saturated water in a PWR-hot leg.

In the following a summary of the test objectives, initial and boundary conditions, test results and major findings is given.

2. UPTF - TEST NO. 12 - SIMULTANEOUS STEAM UP AND WATER DOWNFLOW ACROSS THE UPPER TIE PLATE OF A PWR

2.1 Test Objectives

The objective of test No. 12 was to investigate the simultaneous steam up and local water break through and downflow across the upper tie plate of a pressurized water reactor (PWR). In test No. 12 subcooled water was injected into the intact loop hot legs via the deflector nozzles ("Hutze").

The goal of test No. 12 was to investigate which part of the injected ECC water penetrates into the core region. Of special interest is the location and extension of the water penetration area in the tie plate, the water accumulation and distribution in the upper plenum, the subcooling of the ECC water near the tie plate and the beginning of local water break through related to the start of ECC injection.

2.2 Initial and Boundary Conditions, Test Procedure

All pump simulators in the intact loops were blocked during the test. The broken loop hot leg break valve ($D = 0,75 \text{ m}$) opened at the start of the transient, the broken loop cold leg break valve was closed. The reference value for the pressure in the containment simulator was 4 bar.

The initial water level in the lower plenum of the test vessel was less than 1m and the concentration of non condensibles in the test vessel was less than 100 mg $\text{N}_2/\text{kg H}_2\text{O}$ before start of the transient.

A constant ECC mass flow rate of 400 kg/s was injected into each of the three intact loop hot legs via the deflector nozzle. The ECC water temperature was 30 °C.

To simulate the dissolved nitrogen in the ECC water a mass flow rate of 0.12 kg/s N_2 was injected into the ECC line upstream of each activated injection port.

Steam was injected into the core simulator using a flat radial distribution. The steam injection rate was ramped down from a maximum value of 284 kg/s to a minimum value of 156 kg/s.

An additional steam mass flow rate of 15 kg/s was injected into each of the three intact loop steam generator simulators to avoid water plugs in the intact loop hot legs.

2.3 Test Results

An overview of the test results is presented in Fig. 3. The test transient starts at 147 s (time = 0 is arbitrary) with the start of ECC injection.

Due to the very high steam injection rate into the core simulator (compared to PWR reflood steam production rates) the test vessel pressure is 4 bar higher than the containment pressure at the time ECC is initiated.

The change in test vessel pressure before start of ECC injection is due to the changing steam injection into the test vessel and the opening of the broken hot leg break valve (at 95 s).

Immediately after start of ECC injection

- The collapsed water level in the test vessel shows a substantial increase indicating that ECC water started to penetrate the tie plate despite the high counterflowing steam mass flow rate
- The change in the water level increase at 190 s indicates the start of the test vessel drainage system to keep the water level in the core simulator below 5.3 m.
- All DP-measurements on the tie plate indicate a sharp increase in differential pressure after start of the ECC injection. The measured DP's in the water downflow region indicate not only the collapsed water level but also a part of the momentum of the water impinging on the tie plate. As is shown in figure 4 the maximum DP's were measured in front of the intact loop hot legs in which ECC-water was injected.
- Figure 5 shows that the steam velocity profile near the tie plate measured by the end box turbines is rather uniform before start of ECC injection.
Immediately after start of ECC injection the flow area of the tie plate splits up into a water break through and downflow area and a steam upflow area. Higher steam upflow velocities are measured by the end box turbines after break through because of the reduced steam upflow area (see figure 6).
- The distribution across the tie plate of the measured fluid forces which were acting on the break-through detectors at a fixed time of 200 s are sketched in figure 7.
These profiles show that, in accordance with the end box turbine signals, the injected ECC water broke through the tie plate in front of the injection loops.

The measured fluid forces versus time (see figure 3) indicate that the injected ECC water broke through the tie plate immediately after start of ECC injection and the break through areas did not change their location during the test.
- Examples for steam and water mass flow rates measured by means of the flow modules are plotted in figure 8. These figures indicate that water droplets were carried through the tie plate by the steam in the steam upflow regions and water break through rates up to 30 kg/s/FA have been measured.
- The measured fluid temperatures below the tie plate presented in figure 9 show highly subcooled water temperatures in front of the intact loops immediately after start of ECC injection.

A maximum subcooling of more than 75 K was measured below the tie plate in this test (see Fig. 3).

- A mass balance based on the conventional instrumentation and the DP-wide signals in the upper plenum indicates that the net mass flow rate from the upper plenum through the tie plate into the core region is almost as high as the injected ECC mass flow rate (~ 1200 kg/s) after 170 s in the transient (see figure 10).

2.4 Major Findings

The information supplied by signals from the vertical end box turbines, break through detectors, flow modules and fluid thermocouples near the tie plate provides a consistent picture of the break through behaviour measured in this test.

The main findings are:

- Immediately after start of ECC injection subcooled water reached the tie plate and started to break through into the core region.
- The break through areas established in front of the ECC injection ports did not change their location and remained stable. The break through area itself changed slightly depending on the steam mass upflow rate.
- The tie plate flow area and the core region were split up into a water break through and downflow area below the injecting hot legs and in a steam upflow region. In the steam upflow region the mean steam velocity increased immediately after start of ECC injection because of the reduction of the steam flow area.
- The fluid temperature measurements near the tie plate indicate strongly subcooled water in the break through areas. A maximum sub-cooling of more than 75 K was measured.
- Water break through rates up to 30 kg/s/FA have been measured.

As the steam up-flow rates were much higher than typical steam mass flow rates at core outlet in the end-of-blowdown, refill and reflood phase of a PWR, the conclusion can be drawn that there are no limiting conditions for local water downflow in the upper part of a PWR core during these phases of a LOCA when similar strongly subcooled ECC mass flow rates are injected into the upper plenum.

3. UPTF - TEST NO. 1 FLUID-FLUID-MIXING

3.1 Test Objectives

This test investigated water mixing phenomena which may occur in the downcomer of a pressurized water reactor (PWR) as a result of high pressure coolant injection into the cold leg at a time when the reactor coolant system is at elevated temperature. This mixing relates to the overall reactor safety problem of pressurized thermal shock (PTS).

The objective of this test was to obtain data on the natural mixing of a cold stream of cold leg injected ECC water with stagnant hot primary fluid. Of particular interest were the temperature distributions and associated mixing obtained in the downcomer region. The UPTF geometry is suitable to realistically simulate this complex three-dimensional mixing region (near the downcomer entrance and in the downcomer) which until now has only been investigated in smaller scale tests.

The initial "hot" fluid temperature in UPTF at the start of the test is limited by facility design. Specifically, UPTF is operationally limited to a pressure of 18 bar ($T_{\text{sat}} = 207\text{ }^{\circ}\text{C}$, or $405\text{ }^{\circ}\text{F}$). Since the initial temperature must be below this saturation temperature, a value of $190\text{ }^{\circ}\text{C}$ was chosen. Accordingly, the mixing and cooldown which occurred in this test were "scaled-down" thermally from the phenomena which may occur in a PWR.

Since the data are not at full pressure and temperature, care should be taken in interpreting the results relative to a PWR.

However, the main objectives of this test were satisfied, which were to provide full-scale data, principally for code verification purposes.

3.2 Initial and Boundary Conditions, Test Procedure

The fluid/fluid mixing phenomena was simulated by injecting ECC into a single cold leg ECC nozzle while the vessel and loops were initially full of hot water. This test involved injection only into a single loop, because provisions to inject this low flow exist on only one loop at UPTF. Accordingly, flow in the other loops and "multi-plume" mixing in the downcomer were not covered in this UPTF test.

All UPTF intact loop cold legs were sealed off at the pump simulator to avoid U-tube oscillations of the primary fluid (see Figure 2).

The injection nozzle used to simulate mixing conditions typical of a US PWR injection configuration (injection from the top) is shown in Fig. 11.

The overall test approach in UPTF was to obtain test data for the following four ECC flows, starting at nominally identical initial conditions: 5, 10, 20 and 40 kg/s. Accordingly, the test was run in four phases, with each phase covering the transient response to a single ECC flow.

This range of ECC flows covers the range of possible high pressure injection rates for the US PWR.

An ECC water temperature of 25 to 35 °C was used in these four test phases depending on the temperature of the water supplied by the power station GKM.

The water level in the water separator in the broken loop hot leg was controlled to a constant value during the test by means of a control loop. The UPTF primary fluid water level was 12.7 m referenced to inside bottom of test vessel, that means the test vessel, the cold and hot leg nozzles as well as the pump seals were completely filled with water.

To homogenize the temperature of the UPTF primary fluid before start of the test the primary fluid was heated up and then depressurized to approximately 12.5 bar. When the fluid temperature homogenization was reached the system pressure was increased to 18 bar and kept constant by means of nitrogen injection into the pipes simulating the U-tubes of the steam generators.

There was no core heating or other heating during the test, so each test phase produced a gradual cooldown of the entire system.

3.3 Test Results

This test was run in four phases, each covering the transient response to one of the ECC flow rates 5, 10, 20 and 40 kg/s. As examples the test results for 40 kg/s and for 10 kg/s are described in the following.

The system responses in the cold leg for the ECC injection rates of 40 and 10 kg/s are plotted in figure 12. This Figure shows that after start of the ECC water injection a "cold water stream" originates with the "High Pressure Injection" plume at the point of injection, continues towards both ends of the cold leg and decays away as the resulting plume falls into the downcomer. A "hot water stream" flows counter to this "cold water stream" in the cold leg between injection nozzle and downcomer supplying the flow necessary for mixing (see T/C Stalk No. 3 and 4).

The fluid temperature measured by T/C Stalk No. 6 indicates for 40 kg/s injection rate a well mixed cooldown of the water volume between injection nozzle and closed pump simulator. A T-stratification was measured for 10 kg/s injection rate. Two dimensional contour plots of the fluid temperatures measured in the cold leg at 170 s and 500 s after start of ECC injection are given in figure 13 for both injection rates under consideration. These figures show the time dependence of the fluid stratification in the cold leg between injection nozzle and downcomer.

The system response in the downcomer area for 40 kg/s and 10 kg/s injection rate is depicted in figure 14. For 500 s after start of ECC injection into the cold leg the vertical fluid and wall temperature distribution in the downcomer below the centerline of the cold leg is shown in figure 15 for 40 kg/s and 10 kg/s injection rate.

Two dimensional contour plots (unwrapped downcomer) of the measured fluid temperatures at 170 s and 500 s after start of ECC injection are shown in figure 16 for 40 kg/s and 10 kg/s. These figures show the development of the cold water plume in the downcomer. The nonsymmetry of the contour relative to the cold leg centerline is due to the interpolation between a limited number of fluid temperature measurements in the downcomer and

not a physical effect.

A dimensionless presentation of the vertical fluid temperature distribution in the downcomer below the cold leg centerline for a fixed time (500 s after start of ECC injection) is depicted in figure 17 for all four injection rates used in this test.

These geometrically full scale 18 bar UPTF data together with data from geometrically similar small scale tests at full pressure give an excellent data base for code verification.

3.4 Major Findings

The following phenomena were observed in this fluid-fluid mixing test:

- The cooldown of the fluid in the cold leg between injection nozzle and closed pump simulator followed a "well mixed" transient for 40 kg/s injection rate.
- Fluid temperature stratification was measured in the cold leg between downcomer and injection nozzle indicating the counterflow of a cold water stream flowing to the downcomer on the bottom of the pipe and a hot water stream on top of the pipe.
- A plume of cold water was penetrating down the downcomer below the cold leg center line.

Detailed temperature distribution data in the cold leg and downcomer for validation of numerical, three-dimensional thermal mixing models are provided.

4. UPTF - TEST NO. 11 COUNTER CURRENT FLOW OF STEAM AND SATURATED WATER IN A PWR - HOT LEG

4.1 Test Objectives

The objectives of this test were:

- Investigation of the steam-water counter current flow phenomena in a PWR-hot leg during the reflux condensation mode of a small break LOCA (steam is condensed in the steam generators and the condensate is flowing counter to the rising steam in the hot leg).
- Determination of the counter current flow boundary for steam and saturated water in the PWR-hot leg to verify that there is a significant margin between the PWR reflux condensation conditions and the flooding limit.
- To provide a full scale data base for code assessment.

4.2 Initial and Boundary Conditions, Test Procedure

Countercurrent flow of steam and saturated water in the hot leg of a PWR was investigated in UPTF at 3 and 15 bars by venting steam from the primary system through the UPTF broken loop hot leg to the containment

simulator downstream of the steam/water separator which simulates the steam generator. Simultaneously a stream of saturated water was injected in the steam/water separator inlet plenum. Figure 18 shows the hot leg and the water injection nozzle. The series of mass flow rates applied in this test is listed in table 1. In Run 37 of Test 11, mass flow rates typical for a potential reflux condenser mode in a PWR were injected.

The measured water level increase at quasi-stationary conditions measured in the lower plenum of the test vessel was used to determine the down-flowing water rate. The water level outside the cyclones of the steam/water separator was measured to check the mass balance. The water first mode was used in this test.

4.3 Test Results

The test results are given in Table 1 and presented in figure 19A.

In figure 19B, the experimental correlations of Richter et al. /1/ and Ohnuki /2/ are presented. These correlations based on the Wallis-parameter J^* are drawn from counter current flow experiments on small scale model hot legs.

The Wallis parameter J_k^* is defined by

$$J_k^* = \frac{\sqrt{\rho_k \cdot j_k}}{\sqrt{g \cdot D \cdot \Delta \rho}} \quad k = w, s$$

ρ - Fluid density

g - Acceleration due to gravity

D - Hydraulic Diameter

$$\Delta \rho = \rho_w - \rho_s$$

Subscripts

w - Water

s - Steam

4.4 Major Findings

- Stable water downflow exists at the conditions expected during the reflux condensation stage of a small break loss of coolant accident (see Fig. 19, Run 37).
- The Wallis-parameter-correlations of Richter et al. and Ohnuki based on counter current flow tests in small scale model hot legs are in fairly good agreement with the results of this test. For that reason the Wallis-parameter J^* (modified Froude Number) seems to be the right parameter for the geometrical scaling of counter current flow in a PWR-hot leg.

References

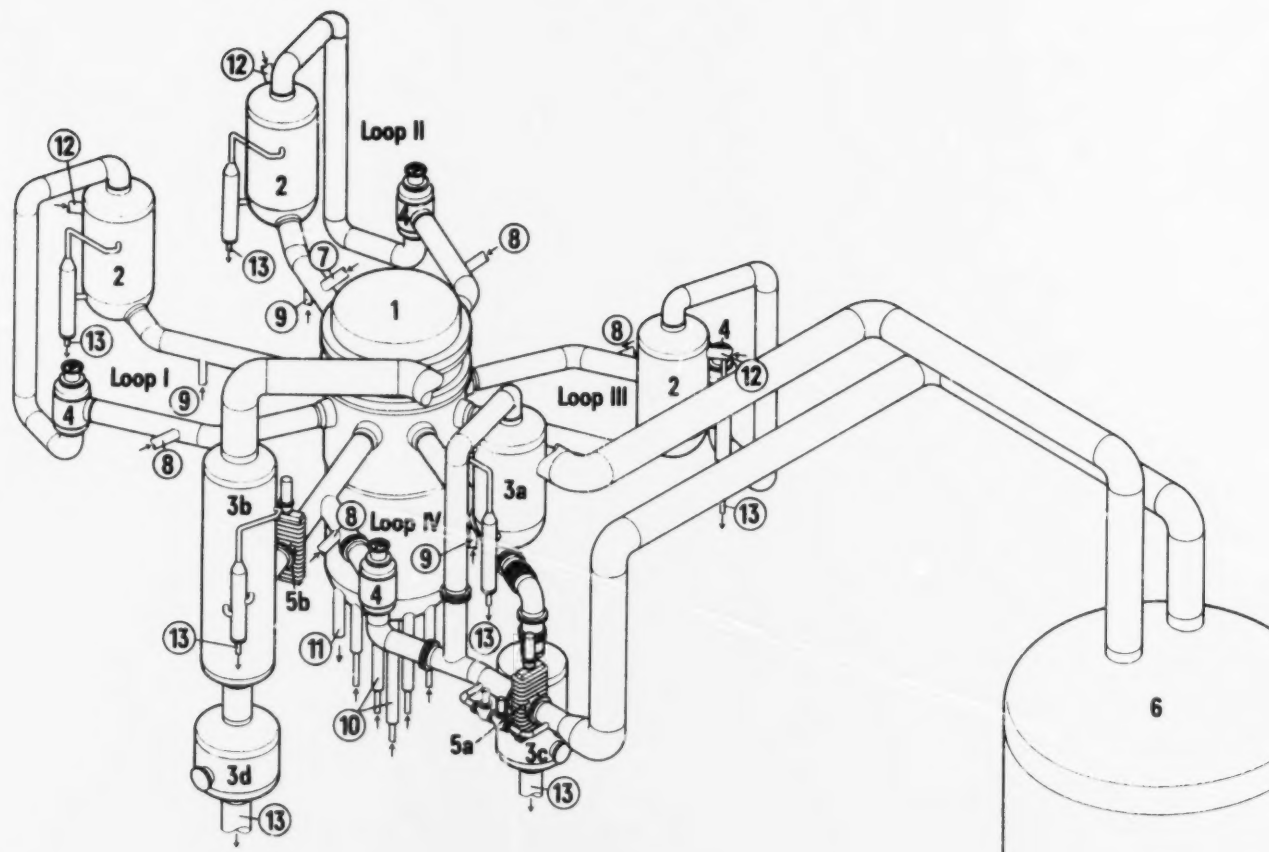
- /1/ "Deentrainment and Countercurrent Air-Water Flow in a Model
PWR Hot leg"
Richter, Wallis, Carter, Murphy
NRC - 0193-9
Thayer School of Engineering
Sept. 1978

- /2/ Ohnuki, A
"Experimental Study of Counter-Current Two-Phase Flow in
Horizontal Tube Connected to Inclined Riser"
Journ. of Nucl. Sci. and Techn., Vol. 23,
No. 3, pp. 219-232, March 1986

SERIES OF FLOW CONDITIONS IN TEST NO.11

UPTF RUN NO.	PRESSURE	INJECTED MASS FLOW RATES		FLOW DIRECTION OF INJECTED WATER
		STEAM	WATER	
	BAR	kg/s	kg/s	
30	3	5	30	DOWNFLOW
31	3	10	30	DOWNFLOW
33	3	11,6	30	DOWN AND UPFLOW
32	3	13,1	30	DOWN AND UPFLOW
30	3	15	30	UP AND DOWNFLOW
34	3	17	30	UPFLOW

UPTF RUN NO.	PRESSURE	INJECTED MASS FLOW RATES		FLOW DIRECTION OF INJECTED WATER
		STEAM	WATER	
	BAR	kg/s	kg/s	
36	15	10	30	DOWNFLOW
38	15	20	30	DOWNFLOW
39	15	27	30	DOWN AND UPFLOW
41	15	31	30	UP AND DOWNFLOW
40	15	35	30	UP AND DOWNFLOW
44	15	36,5	30	UPFLOW
43	15	37,5	30	UPFLOW
42	15	40	30	UPFLOW
45	15	45	30	UPFLOW
37	15	9	10	DOWNFLOW



- 1 Test Vessel
- 2 Steam Generator Simulator (Intact Loop)
- 3a Water Separator (Broken Loop, Hot Leg)
- 3b Water Separator (Broken Loop, Cold Leg)
- 3c Drainage Vessel for Hot Leg
- 3d Drainage Vessel for Cold Leg
- 4 Pump Simulator
- 5a Break Valve (Hot Leg)
- 5b Break Valve (Cold Leg)
- 6 Containment Simulator
- 7 Surge Line Nozzle
- 8 ECC-Injection Nozzles (Cold Leg)
- 9 ECC-Injection Nozzles (Hot Leg)
- 10 Core Simulator Injection Nozzle
- 11 TV-Drainage Nozzle
- 12 Steam Injection Nozzle
- 13 Drainage Nozzle

UPTF - Upper Plenum Test Facility - Primary Loop System

Fig. 1

470

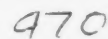
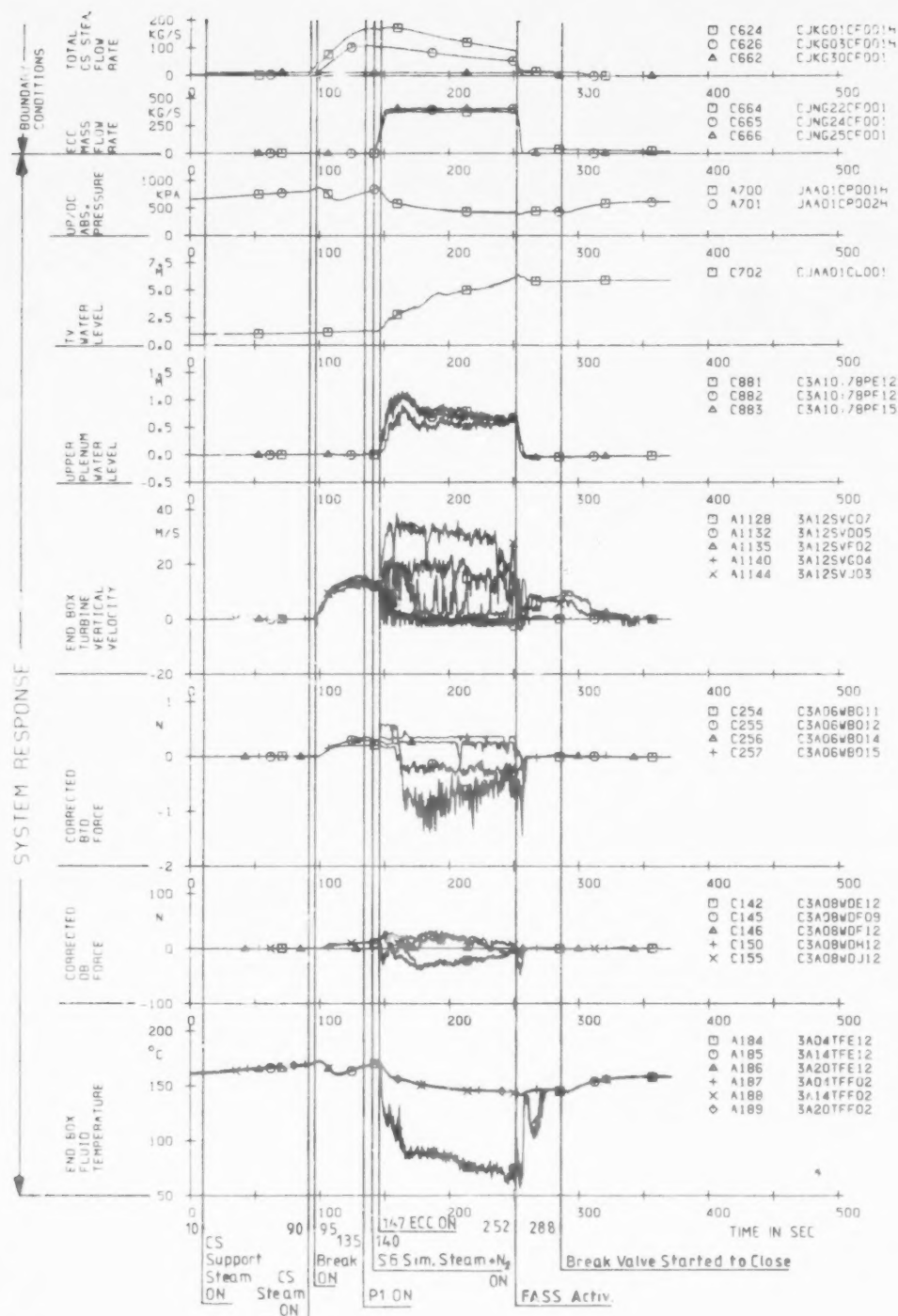


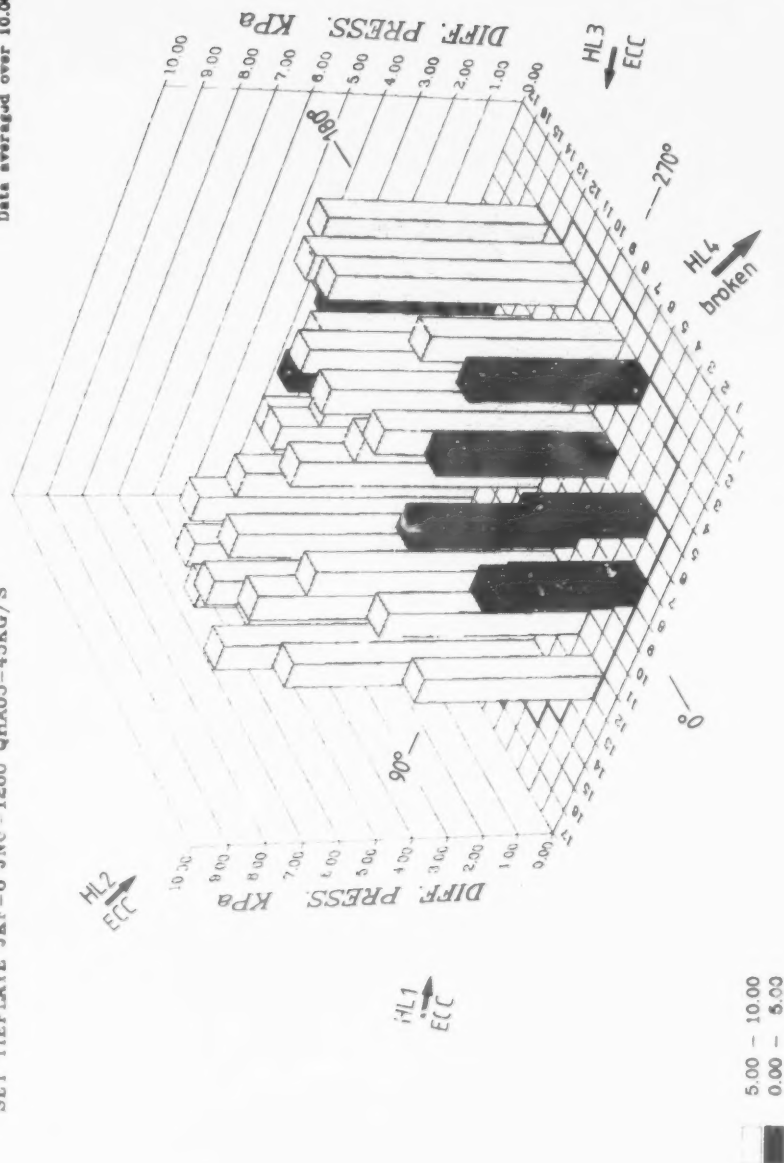
Fig. 2



UPTF - Test Nr.12 - Boundary Conditions and System Response

UPTF TEST RUN 014
SET TIEPLATE JKP=0 JNC=1200 QHA05=45KG/S

Offset from test start: 200 sec
Data averaged over 10.00 sec



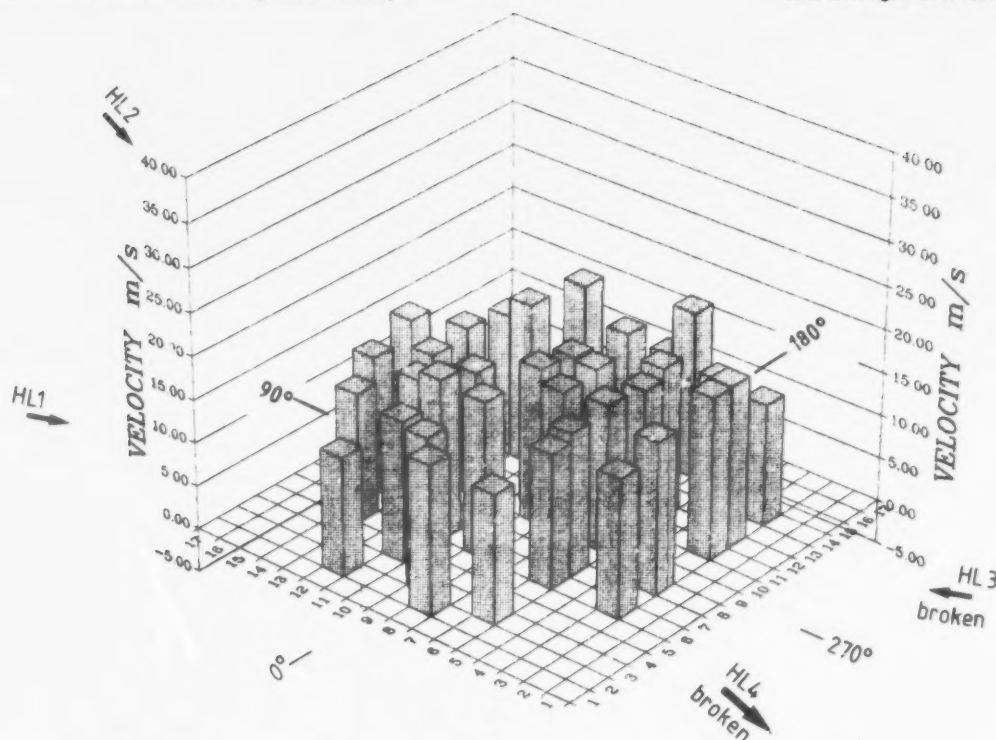
Discrete Values of Wide Range Differential Pressures at 200s

Fig. 4

472

UPTF TEST RUN 014
SET TIEPLATE JKP=0 JNG=1200 QHA05=45KG/S

Offset from test start: 125 sec
Data averaged over 10.00 sec

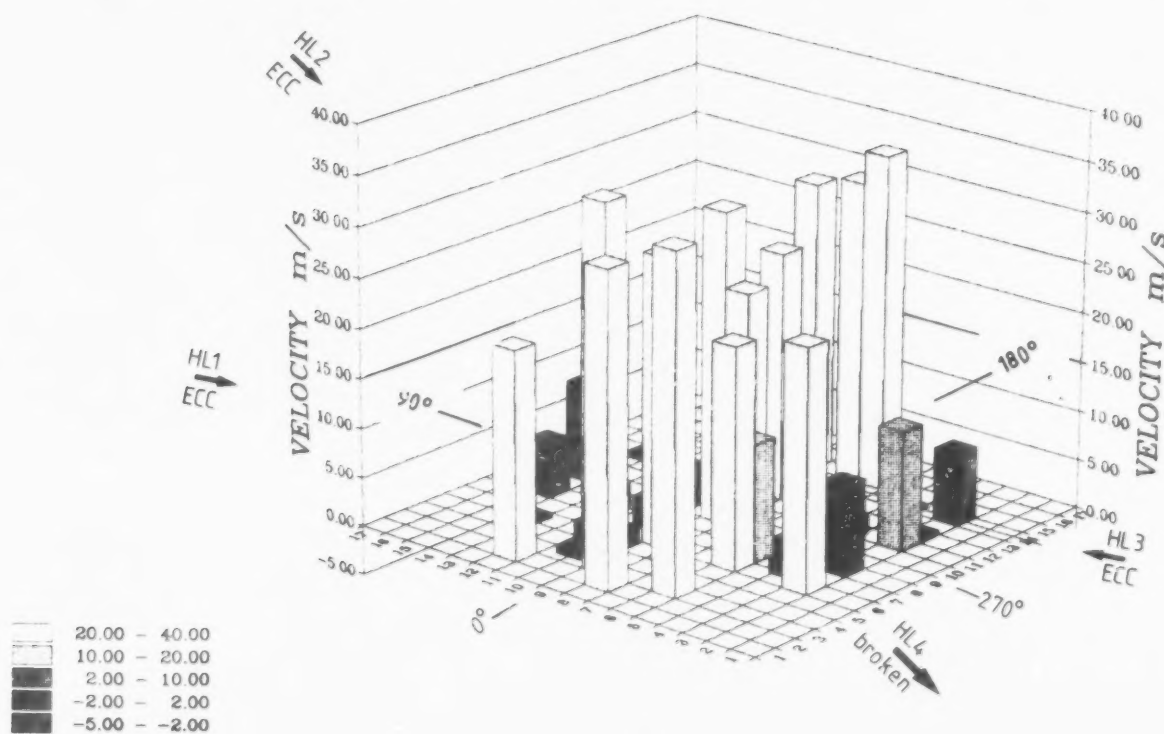


Discrete Values of End Box Turbine Vertical Velocities at 125s

Fig. 5

UPTF TEST RUN 014
SET TIEPLATE JKP=0 JNG=1200 QHA05=45KG/S

Offset from test start: 200 sec
Data averaged over 10.00 sec



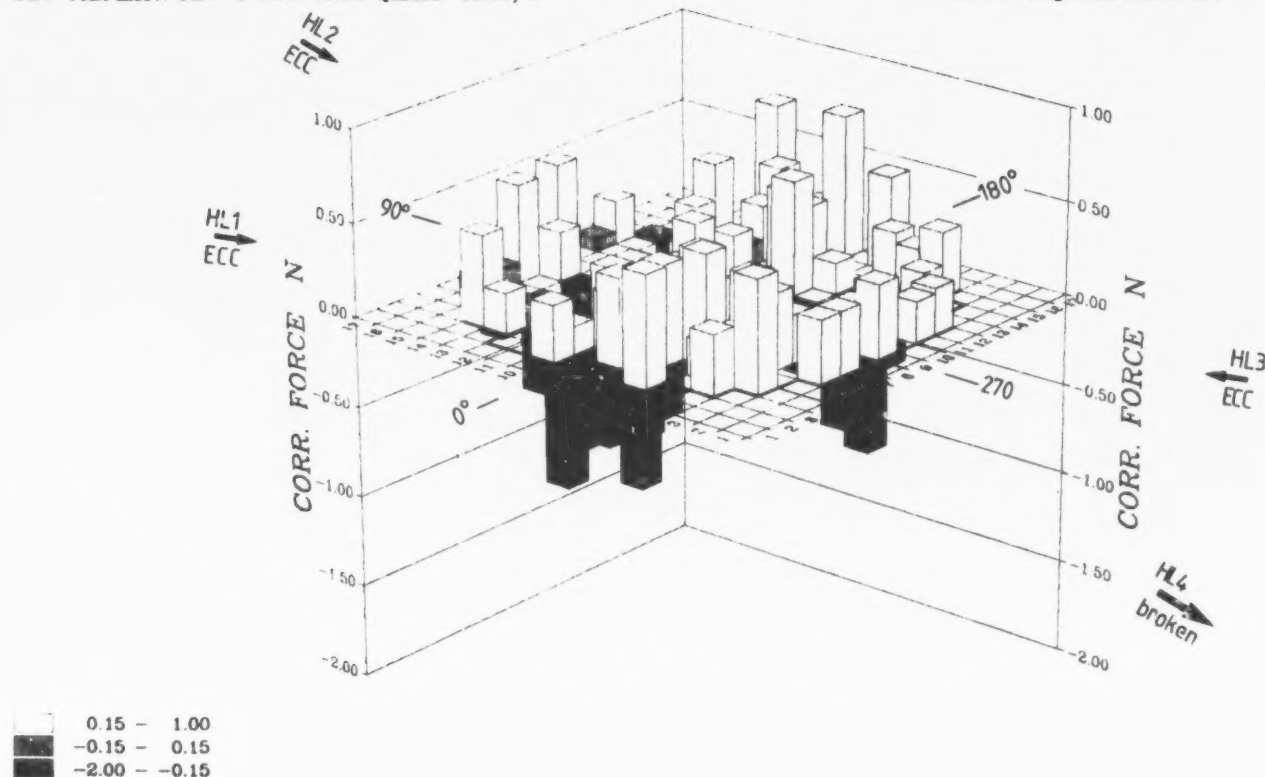
Discrete Values of End Box Turbine Vertical Velocities at 200s

Fig. 6

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UPTF TEST RUN 014
 SET TIEPLATE JK^P=0 JNG=1200 QHA05=45KG/S

Offset from test start: 200 sec
 Data averaged over 10.00 sec



Discrete Values of Corrected BTD Forces at 200s

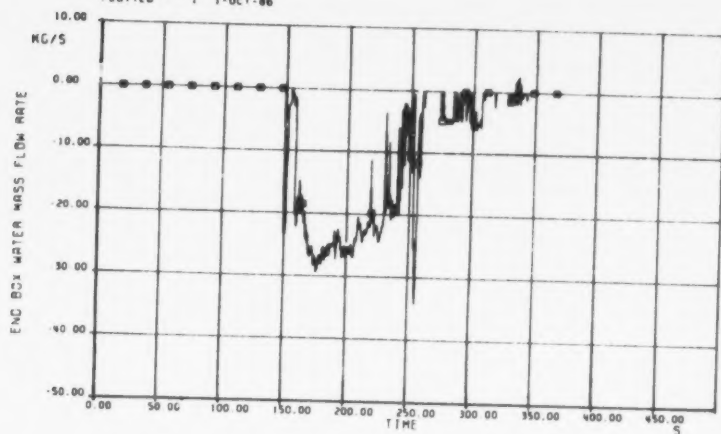
Fig. 7

UPTF TEST RUN 014

MC344 C3A08MFME12 END BOX E12

SET TIEPLATE JKF=0 JNG=1200 QHADS=45KG/S

DATE OF TEST: 18-MAR-88
PLOTTER: 1-OCT-88



End Box E12 - Water Break Through Region

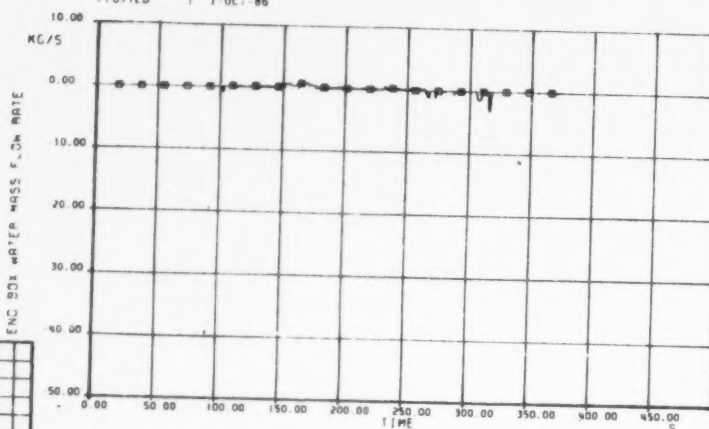
0.15	1.00
0.15	3.15
2.00	0.15

UPTF TEST RUN 014

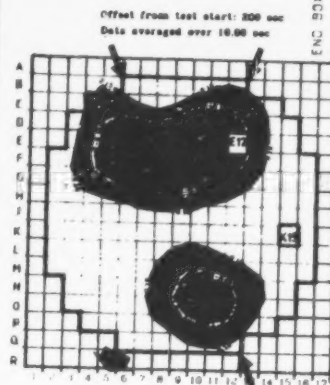
MC360 C3A08MFME15 END BOX K15

SET TIEPLATE JKF=0 JNG=1200 QHADS=45KG/S

DATE OF TEST: 18-MAR-88
PLOTTER: 1-OCT-88

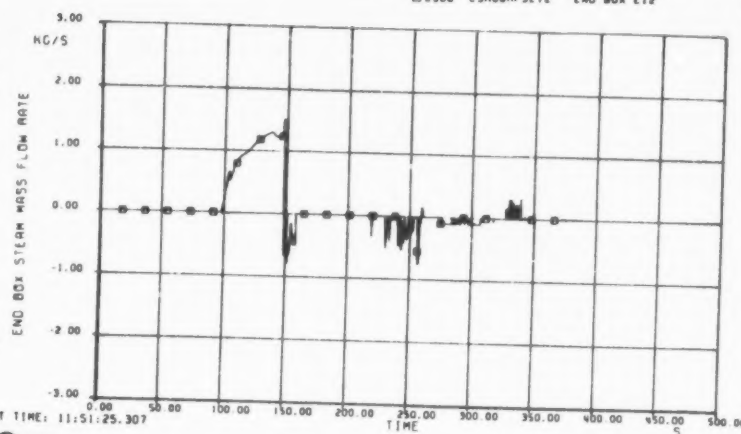


End Box K15 - Steam Upflow Region



Corrected BTD Force in Newton

MC308 C3A08MFSE12 END BOX E12



Flow Module Steam and Water Mass Flow Rates

MC324 C3A08MFSE15 END BOX K15

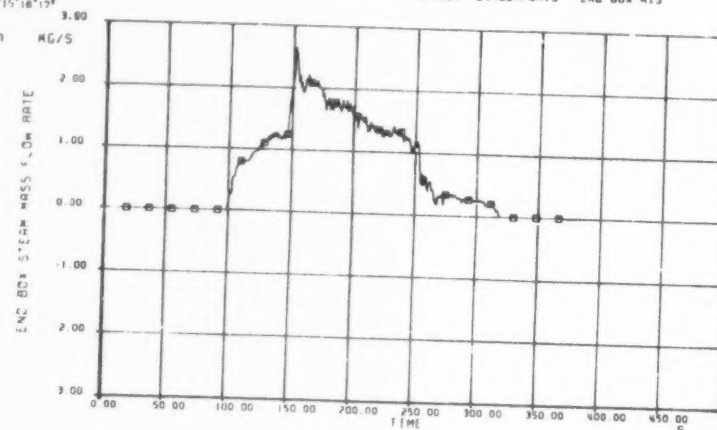


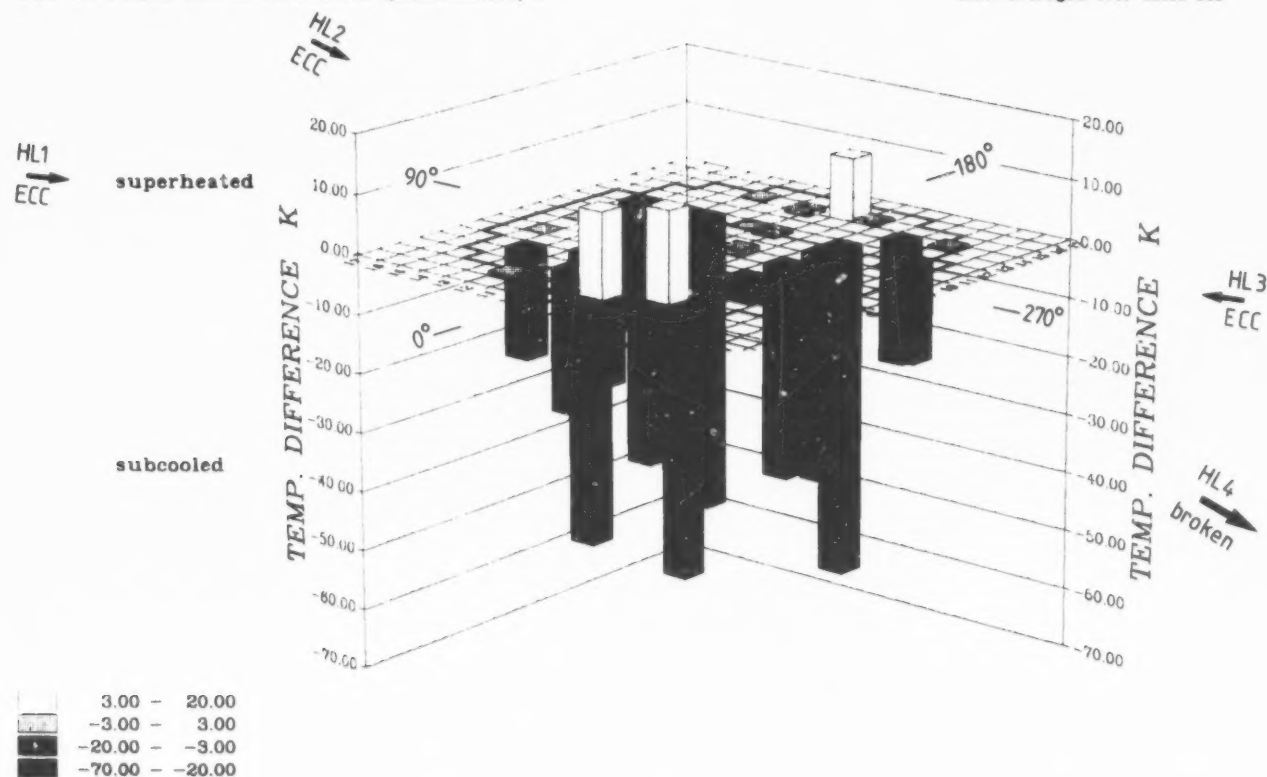
Fig. 8

UPTF TEST RUN 014

SET TIEPLATE JKF=0 JNG=1200 QHAD5=45KG/S

Offset from test start: 200 sec

Data averaged over 10.00 sec



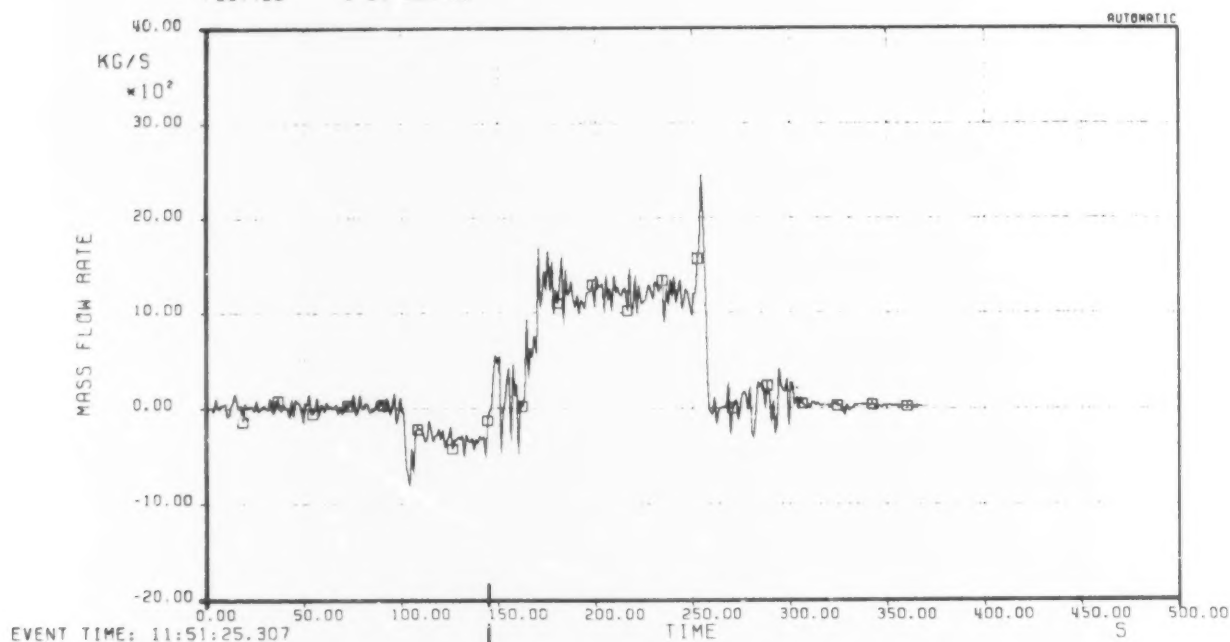
Discrete Values of Fluid Temperatures 10 mm below Tie Plate at 200s

UPTF TEST RUN 014

□ C902 (UP-CORE) 11JAA018B01

SET TIEPLATE JKX=0 JNC=1200 QHROS=45KG/S

DATE OF TEST: 18-MAR-86
PLOTTED : 24-JUL-86



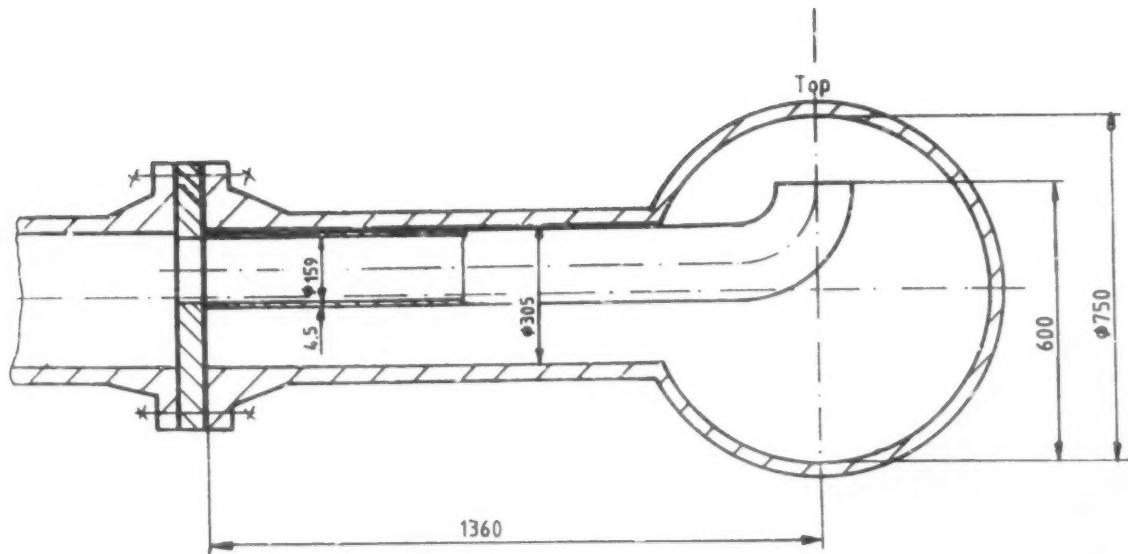
Net Mass Flow Rate from Upper Plenum into Core

Fig. 10

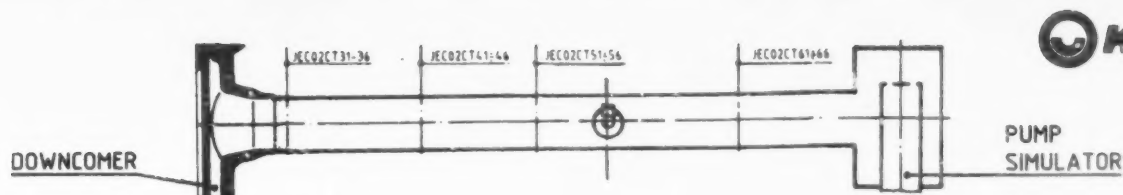
477

477

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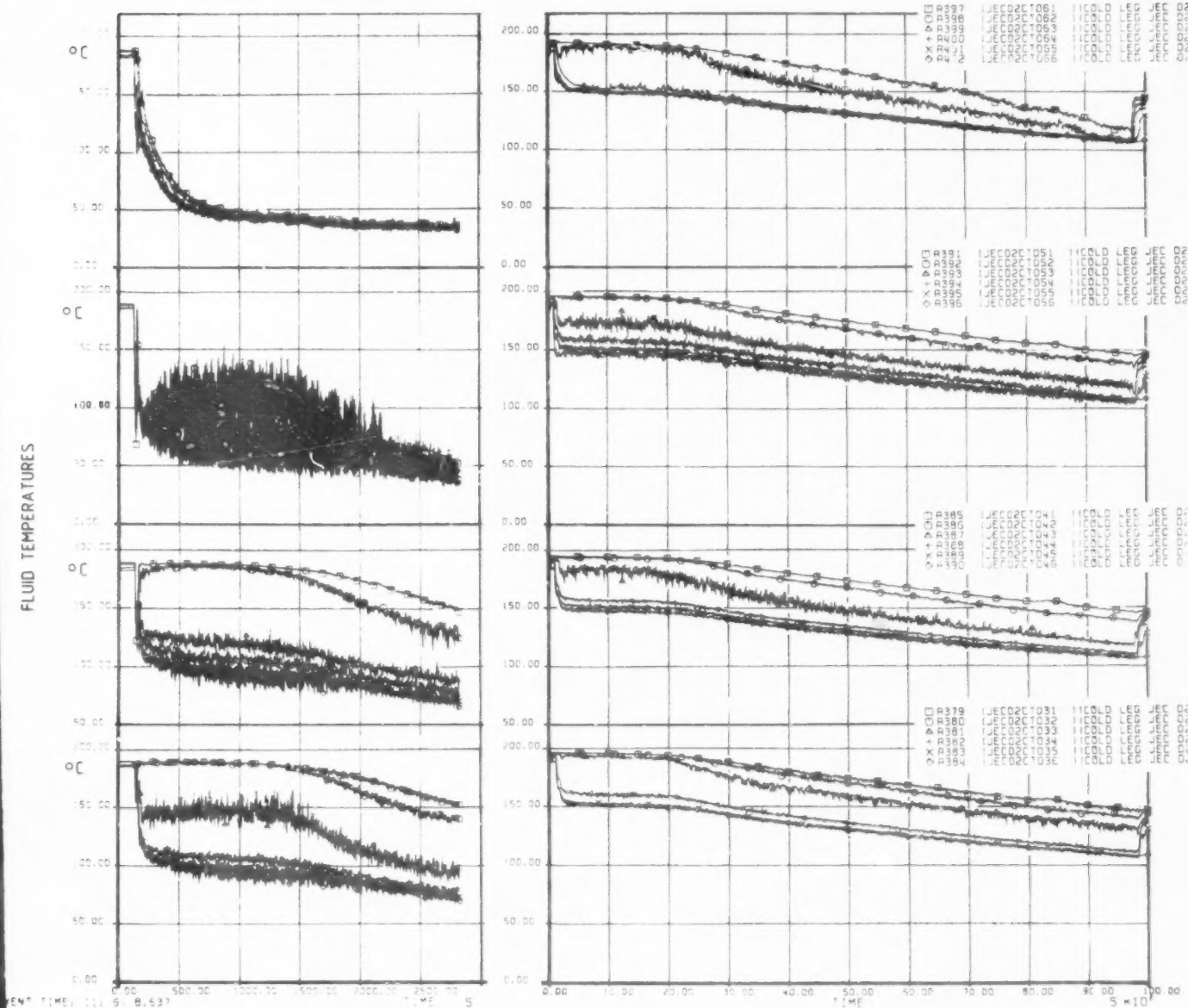


UPTF - High Pressure ECC Injection Nozzle

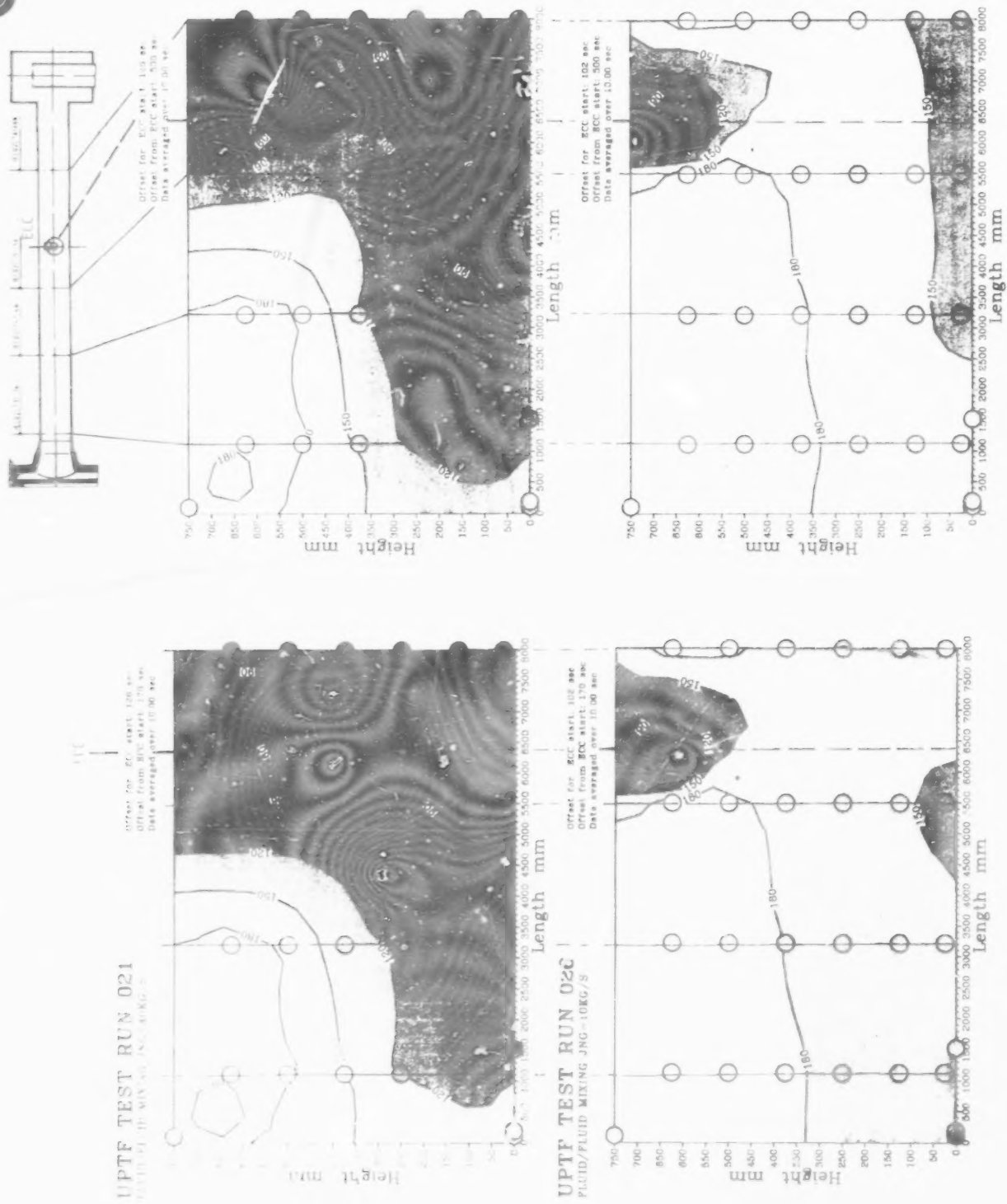


UPTF TEST RUN 021
FLUID/FLUID MIXING JNG=40KG/S

UPTF TEST RUN 026
FLUID/FLUID MIXING JNG=10KG/S



UPTF - System Response in the Cold Leg
Nozzle Area



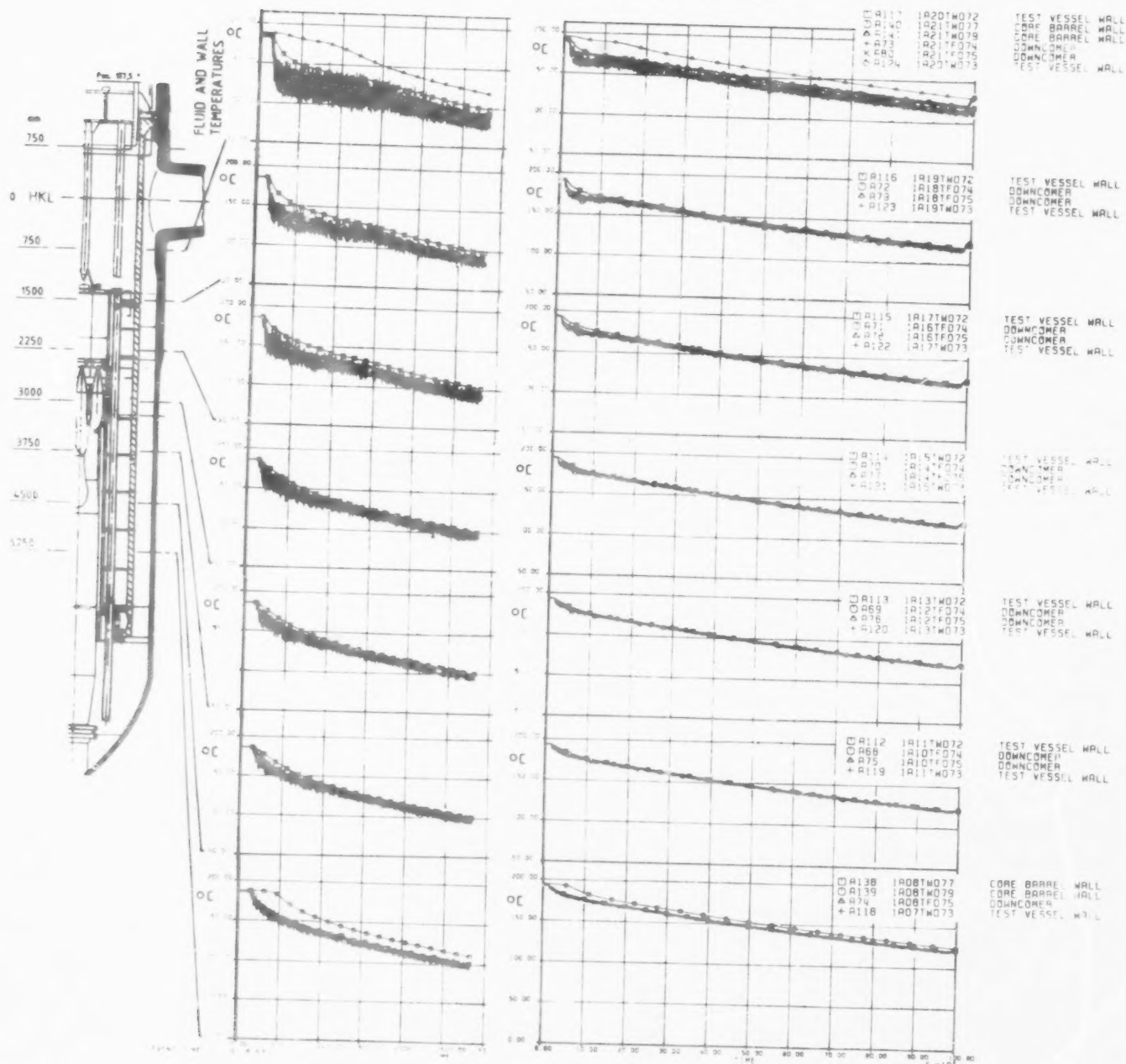
Fluid Temperature Distribution in Loop 2

Fig. 13

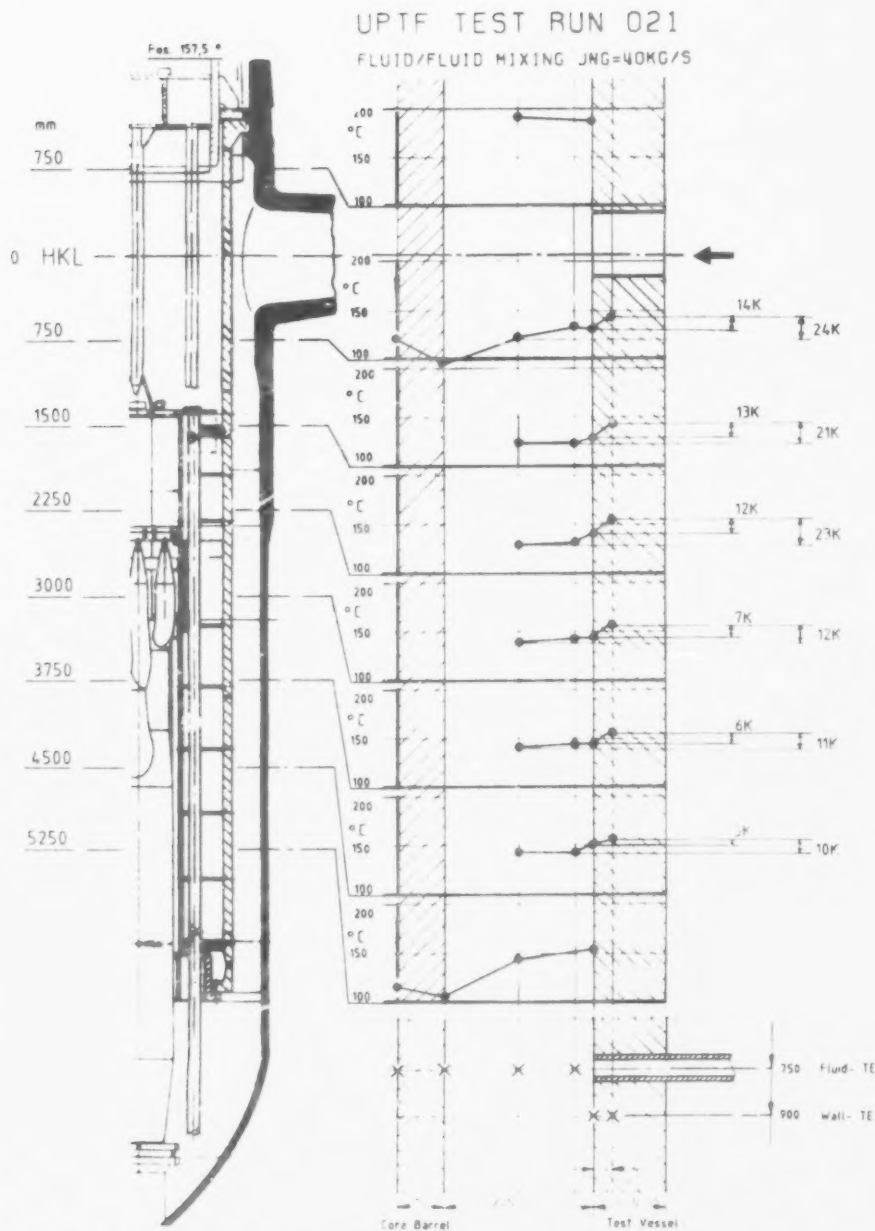
780

UPTF TEST RUN 021
FLUID/FLUID MIXING JNG=40KG/S

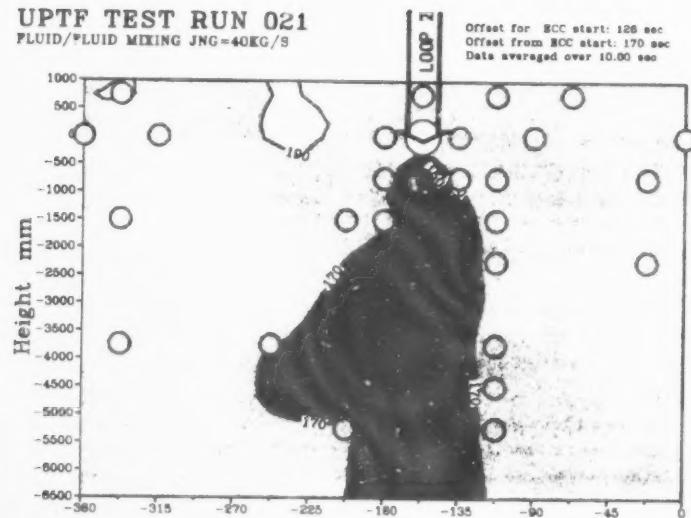
UPTF TEST RUN 026
FLUID/FLUID MIXING JNG=10KG/S



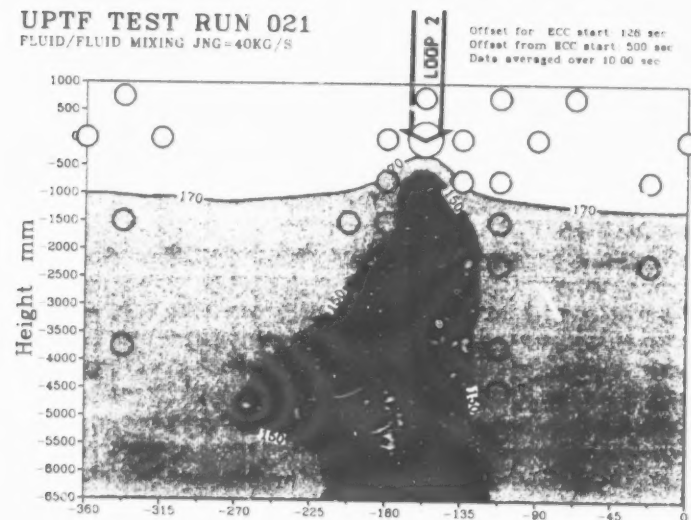
UPTF - System Response in the
Downcomer Area



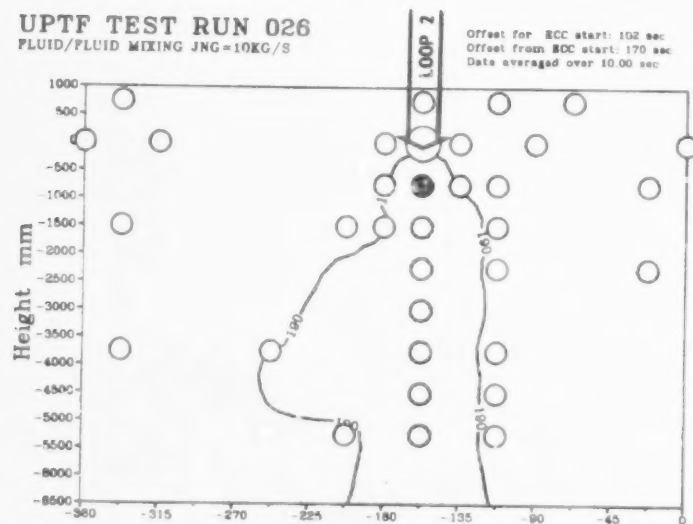
UPTF TEST RUN 021
FLUID/FLUID MIXING JNG=40KG/S



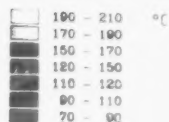
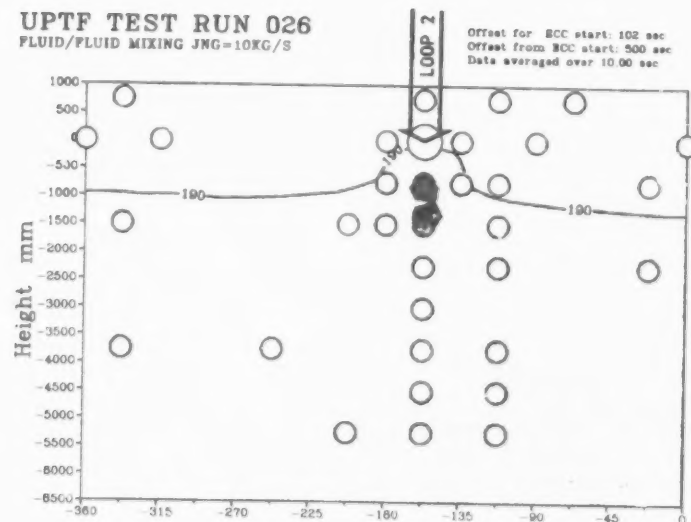
UPTF TEST RUN 021
FLUID/FLUID MIXING JNG=40KG/S



UPTF TEST RUN 026
FLUID/FLUID MIXING JNG=10KG/S

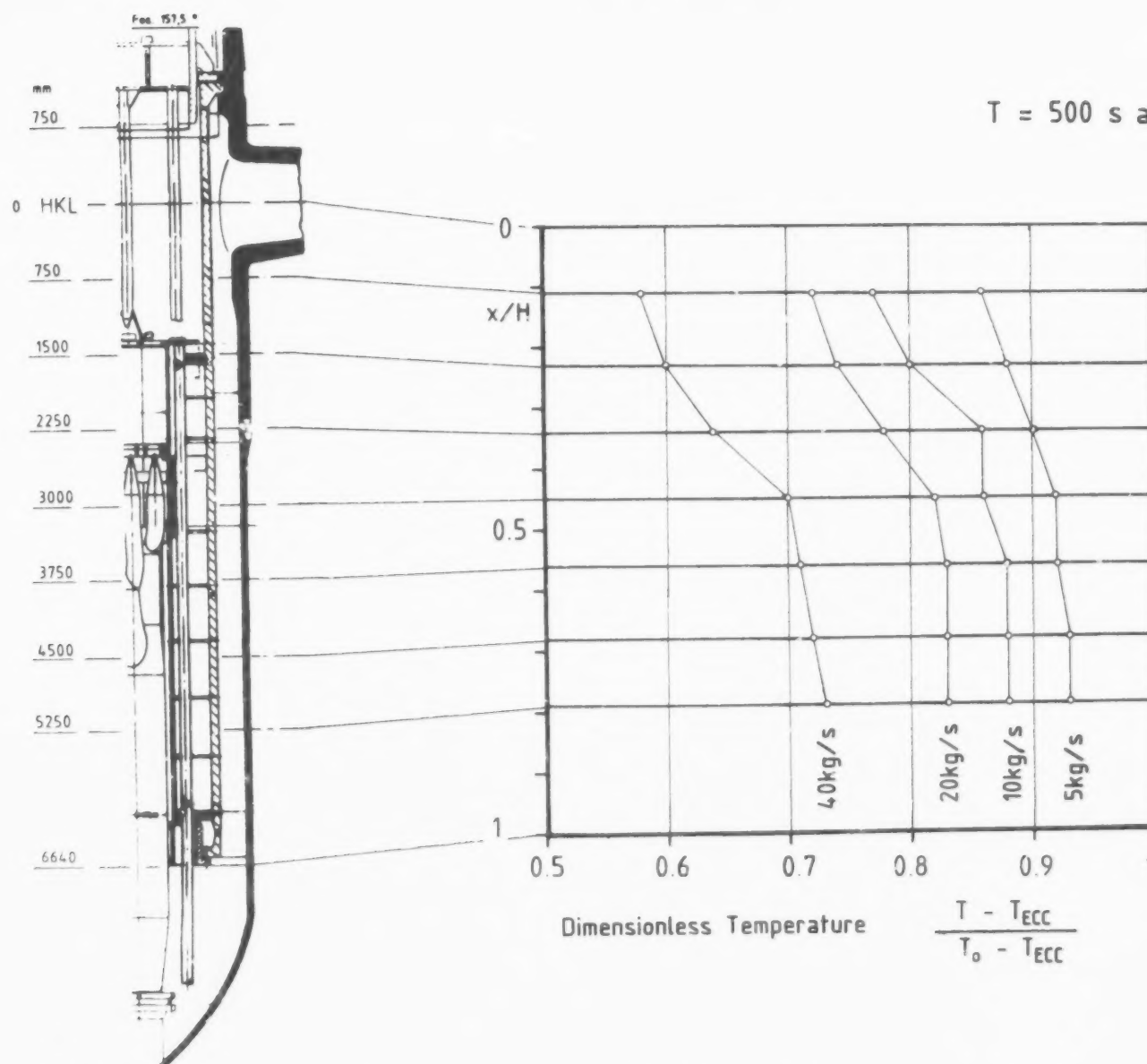


UPTF TEST RUN 026
FLUID/FLUID MIXING JNG=10KG/S



UPTF - Fluid Temperature Distribution in Downcomer
- DC Centerline

Fig. 16



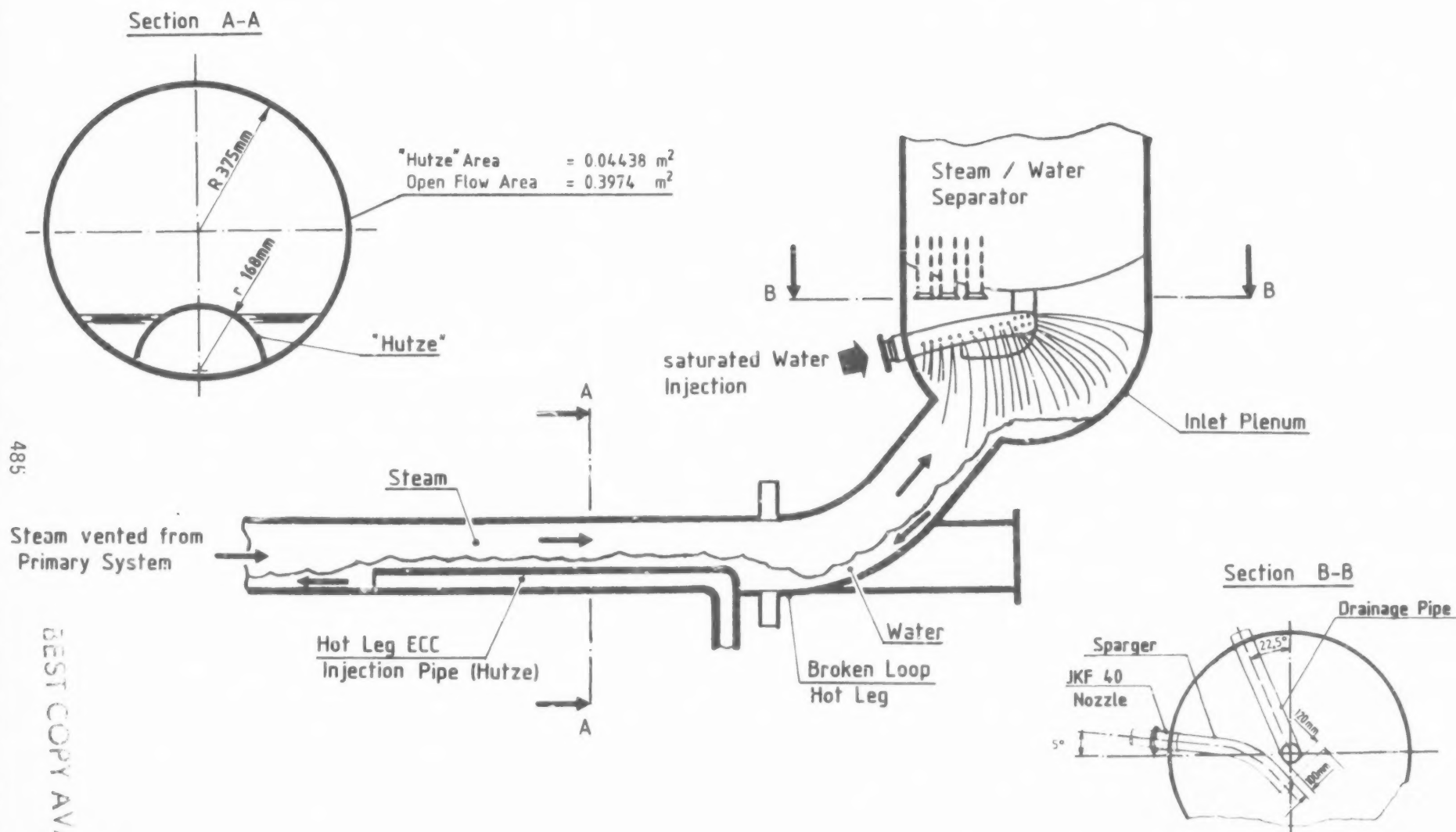
T_{ECC} - ECC Fluid Temperature
(25 - 35 °C)

T_0 - Temperature of
Stagnant Fluid in DC
at Start of Test

T - Fluid Temperature in DC

UPTF - Dimensionless Presentation of Fluid Temperatures
along the DC Centerline

Fig. 17



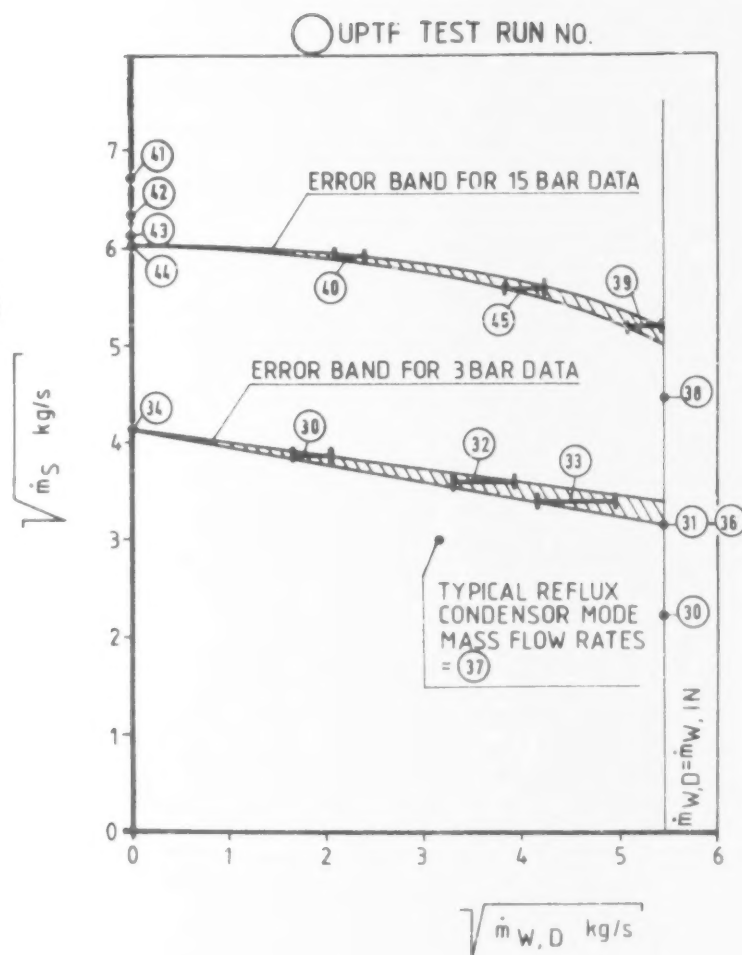
UPTF- Hot Leg Countercurrent Flow

Fig. 18

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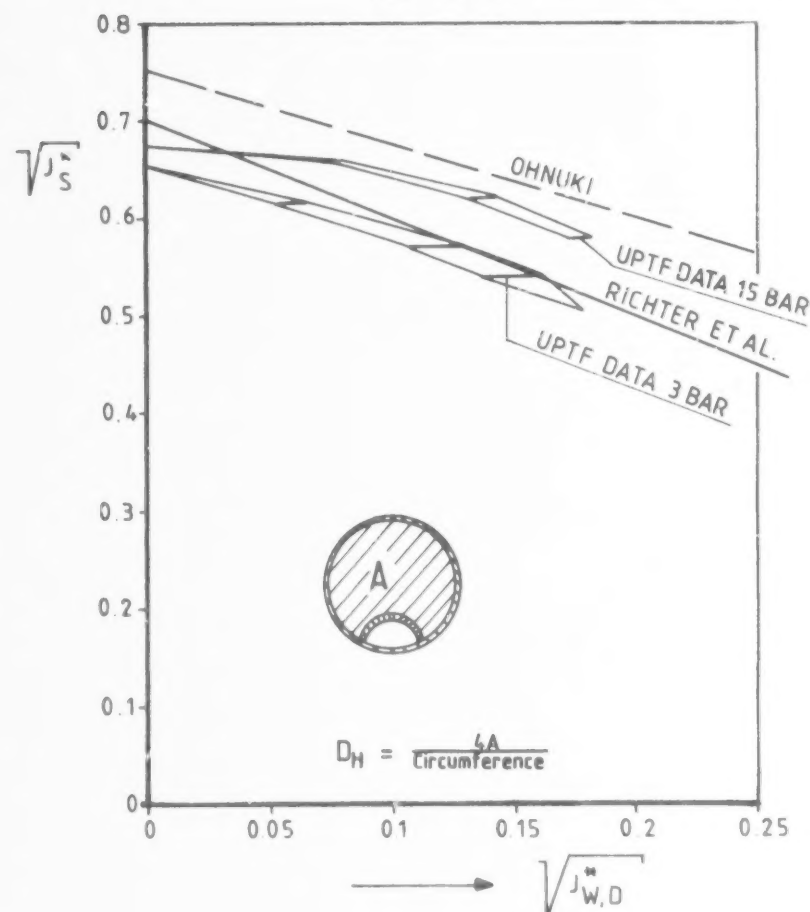
485

HYDRAULIC DIAMETER $D_H = 0.639 \text{ m}$
 FLOW AREA $A = 0.3974 \text{ m}^2$



UPTF - Test Nr. 11 - CCF of Steam and Saturated Water in Hot Leg

Fig. 19A



Comparison of UPTF Data with the Correlations of Ohnuki and Richter et al.

Fig. 19B

ANALYSIS RESULTS FROM THE LOS ALAMOS 2D/3D PROGRAM*

by

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H. Stumpf, P. Shire, J. Gilbert, and J. Hedstrom

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Los Alamos, New Mexico 87545

ABSTRACT

Los Alamos National Laboratory is a participant in the 2D/3D program. Activities conducted at Los Alamos National Laboratory in support of 2D/3D program goals include analysis support of facility design, construction, and operation; provision of boundary and initial conditions for test facility operations based on analysis of pressurized water reactors; performance of pretest and posttest predictions and analyses; and use of experimental results to validate and assess the single- and multidimensional nonequilibrium features in the Transient Reactor Analysis Code (TRAC). During Fiscal Year 1986, Los Alamos conducted analytical assessment activities using data from the Cylindrical Core Test Facility and the Slab Core Test Facility. Los Alamos also continued to provide support analysis for the planning of Upper Plenum Test Facility experiments. Finally, Los Alamos either completed or is currently working on three areas of TRAC modeling improvement. In this paper, Los Alamos activities during Fiscal Year 1986 are summarized; several significant accomplishments are described in more detail to illustrate the work activities at Los Alamos.

INTRODUCTION

The 2D/3D program is sponsored jointly by Japan, the Federal Republic of Germany, and the United States (US). The safety-related objectives of the 2D/3D program are as follows.

1. To provide an improved understanding of the effectiveness of various emergency core-cooling systems (ECCS) in limiting peak fuel rod cladding temperatures during vessel refill and core reflood for medium- to large-break loss-of-coolant accidents (LOCAs) in pressurized water reactors (PWRs).
2. To reveal core coolant inventory and system flow characteristics during the refill and reflood phases of a medium- to large-break LOCA.
3. To study convective flow and temperature distributions inside a heated core during reflood for a medium- to large-break LOCA.

* Work performed under the auspices of the US Nuclear Regulatory Commission.

4. To assess the predictive capability of best-estimate computer codes and the conservatism of evaluation model computer codes.
5. To obtain information which may be used to improve thermal-hydraulic models in best-estimate, evaluation-model, and other computer codes.

Activities conducted at Los Alamos National Laboratory in support of 2D/3D program goals include analysis support of facility design, construction, and operation; provision of boundary and initial conditions for test facility operations based on analysis of PWRs; performance of pretest and posttest predictions and analyses; and use of experimental results to validate and assess the single- and multidimensional nonequilibrium features in the Transient Reactor Analysis Code (TRAC).

Three experimental facilities provide data to 2D/3D program participants. The Cylindrical Core Test Facility (CCTF) is an approximately 1/21-scale facility located in Japan; this facility has completed its test program and the Los Alamos counterpart analysis program is nearing completion. The Slab Core Test Facility or SCTF is a separate-effects reflood facility also located in Japan. A full-height 1/21-scale section of the core, one fuel element wide from core centerline to outer periphery, is modeled. This facility began testing with its third electrically heated core during 1986; Los Alamos will complete analysis of Core-II tests in 1986 and will begin analysis of Core-III tests in FY-1987. The Upper-Plenum Test Facility (UPTF), located in the Federal Republic of Germany, is a 1/1-scale integral test facility focusing on phenomena in the downcomer, lower plenum, upper plenum and primary-system loops of a PWR. Los Alamos analytical efforts to date have largely supported test design and specification; posttest analyses of UPTF experiments will be started as soon as data become available.

During Fiscal Year 1986, Los Alamos conducted analytical assessment activities using data from the CCTF and the SCTF. Los Alamos also continued to provide support analysis for the planning of UPTF experiments. Finally, Los Alamos either completed or is currently working on three areas of TRAC modeling improvement. In this paper, Los Alamos activities during Fiscal Year 1986 are summarized; several significant accomplishments are described in more detail to illustrate the work activities at Los Alamos.

CCTF PROGRAM SUPPORT

We issued our summary report to document results from TRAC-PD2 (Ref. 1) analyses of the Core-I test series (Ref. 2). TRAC was able to consistently predict peak cladding temperatures that were in moderate agreement with the measured values. We define moderate agreement to mean that TRAC correctly predicted the major trends and phenomena that are dominant in determining the peak cladding temperature. Although TRAC did not predict all measured quantities uniformly well, it was able to properly characterize the parametric impact on peak cladding temperature and quench-front propagation displayed by the experimental test series.

We completed our analysis of five Core-II upper-plenum-injection (UPI) experiments and prepared a draft report summarizing our findings (Ref. 3). The objective of the UPI test series in CCTF Core-II was to investigate the effectiveness of the UPI system under a variety of boundary and initial conditions. The parameters varied were core power, core stored energy, core radial power profile, UPI flow, UPI symmetry, system pressure, and the addition of the refill phase. Associated with this objective was the desire to improve understanding of the unique characteristics of core reflood using a UPI ECCS. The phenomena included channeling

of emergency core-cooling (ECC) liquid from the upper plenum into the core, multidimensional characteristics during reflood, steam binding, and negative core inlet flow.

A total of five UPI tests were performed in CCTF Core II. These tests were designed to investigate the effectiveness of the UPI ECCS under large-break LOCA conditions. Table 1 summarizes the assessment results for each of the UPI calculations. Each test was analyzed with TRAC-PF1/MOD1 (Ref. 4) to assess the predictive capability of the code and to identify model deficiencies. Because the CCTF facility is large scale, we believe that the phenomena observed during the UPI tests will also be seen in an actual PWR with UPI. Also, information is provided to support UPI-PWR licensing activities.

The TRAC predictions for four of the five UPI tests, Runs 59, 72, 76, and 78, are in overall moderate agreement with the data (Refs. 5-8). The prediction for the fifth test, Run 57 (Ref. 9), showed insufficient agreement with the data. We define insufficient agreement to mean that major trends and phenomena were not predicted correctly. A nodding study was also performed on Run 57 in an attempt to increase the accuracy of the prediction. These results did not show a marked improvement. The deficiency in predicting Run 57 is not serious since the boundary and initial conditions for the test (*i.e.*, very high core stored energy, and UPI flows lower than the single-failure criteria) are outside even the most conservative assumptions for a UPI PWR. Therefore, the summary conclusions relative to TRAC applications listed below are based on the assessment activities for CCTF Runs 59, 72, 76, and 78; these are the tests with initial and boundary conditions within the achievable envelope of accident conditions in actual PWRs.

The core reflood transient for the UPI tests in CCTF Core II is characterized by strong multidimensional flows. Flow of the UPI fluid into the core is asymmetric, and tends to "channel" down one side or the other. The downward liquid flow causes local cooling of the rods and in some cases complete quench very early in the transient. This flow supplements the quench front progressing from the bottom. Steam production in the core is routed away from the channeling area in sufficient quantity to cause local complete flooding (*i.e.*, no liquid downflow). As liquid accumulates in the core, the UPI flow from the upper plenum causes the net core inlet flow to be negative. The core liquid level is established by the balance of the downcomer level and the pressure drop through the system as in the case of cold-leg injection reflood. Other features that are unique to the UPI ECCS are the pool formation and condensation in the upper plenum. Compared to a cold-leg injection configuration, the CCTF Core-II UPI results show that there is a reduction in peak cladding temperature as a result of the UPI. Although the reduction in the peak cladding temperature is small, this comparison shows that this particular alternative ECCS is equally effective as cold-leg injection.

With respect to qualitative results, we found that TRAC was consistently able to predict (1) the channeling effect, (2) the asymmetric core reflood, (3) the negative core inlet flow, and (4) the breakthrough location. In addition, the TRAC-calculated rod surface temperatures were in moderate agreement with the data. Overall, the TRAC-calculated quantitative results were sufficiently close to the data to be characterized as in moderate agreement. It is difficult to draw definitive conclusions about the impact of parametric variations for the UPI test series because more than one parameter was varied for each test. However, in general the effects of core power, core stored energy, UPI flow, and UPI symmetry were calculated.

The overall effectiveness of the UPI ECCS was assessed by comparison to a CCTF Core-II cold-leg-injection experiment of similar power and stored energy. The test results show that a slight reduction in the peak cladding temperature occurs as a result of the UPI. TRAC is able to predict this trend. Although the reduction in peak cladding temperature is slight, the results show that the UPI provides adequate core cooling during the assumed accident conditions.

As a consequence of our UPI assessment activities, several improvements in the TRAC code have been requested. These are (1) modeling of condensation in the cold leg, (2) prediction of liquid downflow rate from the upper plenum into the core, (3) prediction of core void fraction distribution in the region of vapor upflow, and (4) entrainment/deentrainment in the upper plenum (related to the flow regime map implementation). With respect to item 1, improvements to the condensation model are planned to be released with TRAC-PF1/MOD3. For item 2, a CCFL option has been incorporated in TRAC-PF1/MOD1 with EC 12.9. For items 3 and 4, improvements in the core void distribution and entrainment/deentrainment will be included in TRAC-PF1/MOD2 (late FY 87 release). For one study, we also examined the relationship between noding practice and the prediction of breakthrough location. We found that for very low UPI flows, a much finer noding (doubling of azimuthal sectors from four to eight) was required to produce a moderate agreement in breakthrough location. User guidelines have been summarized and recommendations made with respect to the noding detail and the UPI input model.

TRAC is able to predict the major phenomena associated with UPI reflood transients and quantitatively predict the rod temperatures. An important TRAC capability is the ability to model and predict the important multidimensional flows that occur in UPI facilities during reflood transient. It is believed, therefore, that to adequately represent the dominant reflood phenomena, a multidimensional prediction capability is required. We believe that TRAC-PF1/MOD1 can be used with confidence to predict UPI transients in actual plants assuming sufficient detail is incorporated in the facility input model.

We also completed our analysis of Run 71 (Ref. 10), the single best-estimate test run in the CCTF. Significant phenomena observed in this test included long-period hydraulic oscillations in the core which caused overcooling of the lower core and dryout and reheat of the upper levels of the core. TRAC also predicted core oscillations but with a shorter period and smaller amplitude. TRAC predictions of average peak cladding temperatures, heater rod behavior in the lower levels of the core, and overall loop pressure drop were in moderate agreement with measurements.

SCTF PROGRAM SUPPORT

We prepared our summary report to document results from TRAC-PD2 analyses of the Core-I test series (Ref. 11). This test series showed that the impacts of radial power peaking and "hot channel" factors are mitigated by multidimensional hydrodynamics resulting in a "chimney" cooling effect in the high-power bundles. For this series, TRAC-PD2 was able to accurately predict both the hydrodynamic and heat-transfer phenomena during the core reflood phase. In particular, there was moderate agreement between the predicted and measured peak cladding temperatures under blind posttest conditions for a spectrum of parametric experimental tests. We have completed one operational study using TRAC-PF1/MOD1 in support of the initial phase of SCTF Core III testing.

We have completed a detailed study of the SCTF upper-plenum liquid accumulation with special-purpose TRAC input models. Typically, for cold-leg injection-type reflood transients in SCTF, we have underpredicted the amount of liquid accumulation and its distribution in the upper plenum (Fig. 1), and the amount of liquid accumulation in the hot leg. To investigate the reason for this difference, we developed a stand-alone model of the upper plenum, and imposed steam and liquid boundary conditions that were developed from the experimental data. Although there is a large uncertainty in the data for the amount of liquid exiting the core, we found that given these conditions, the prediction for the upper-plenum liquid accumulation was in much better agreement with the data (Fig. 2). From this result, we concluded that the upper-plenum deentrainment was predicted reasonably well by TRAC, and that the reason for the previous difference between the prediction and the experiment for the amount of upper-plenum liquid accumulation was the lack of liquid carryover from the core region.

To investigate the reason for the upper-plenum liquid distribution that is observed in the data (Fig. 1), we did a series of noding studies with our stand-alone upper-plenum model. We found that with sufficiently fine noding we could obtain a distribution of liquid in the upper plenum similar to the experimental data. We determined that the distribution was at least partially caused by the Bernoulli effect (velocity pressure-lift effect), which occurs as the vapor velocity increases as it enters the hot leg from the upper plenum.

To investigate the reason for the underprediction of liquid accumulation in the hot leg, we developed a stand-alone input deck for this component, and imposed boundary conditions in a method similar to that used in the upper-plenum study. The SCTF hot-leg geometry is atypical of reactor hardware since it is rectangular in geometry, 0.116 m wide and 0.737 m tall. In contrast, the constitutive relations used in TRAC were developed from circular-pipe data. Thus, as a parametric study, we modified the constitutive relations for the interfacial drag. We found that changes to the interfacial drag model in TRAC had a very small effect on the deentrainment results. The largest effect occurred when the slip ratio for the inlet conditions was increased. We believe, therefore, that this is an area that requires further examination, caused by the nonprototypical geometry of the SCTF upper plenum and hot leg. Care must be taken in drawing conclusions regarding the predictive capability of TRAC.

UPTF PROGRAM SUPPORT

During this period, we continued to provide support analysis for the planning of UPTF tests. Although actual tests have been completed in the facility, the test data are not yet available and therefore posttest analyses have not been attempted. To date we have completed the pretest analysis of three planned tests in the facility that are of special interest to the US participants: (1) a downcomer separate effects test (DC-SET), (2) a small break LOCA hot-leg counter-current flow limitation (CCFL) test, and (3) a US/Japan (J) PWR large-break LOCA integral test. Work also continued on improving the input decks for the facility. Improvements include the modeling of the subcooled injection system recently added to the facility and the addition of the nitrogen-injection systems to the intact-loop ECC-injection nozzles.

Analysis of the possible loads on the instruments in the facility is receiving special attention. In light of the recent damage to the drag disks in the loops and the degradation of many of the instrument signals, increased efforts are being made to avoid further potential damage. In working closely with consultants from MPR Associates Inc., we have identified

the potential for damage to the downcomer level detectors in the upcoming DC-SET tests. Possible alternatives are being assessed to mitigate this potential.

In the case of the hot-leg CCFL test, the comparisons against a set of possible correlations is acceptable. Also, the instrument loads are well within the limits of the facility. Thus, this particular test is expected to yield useful data, and not cause any operational problems.

For the US/J integral test, a procedure was developed for the start of the test that will meet the target values for several system parameters. Since this test is initiated at a high system pressure of 10-18 bar, the facility will blow down to containment pressure after the opening of the break. The pretest calculation was continued for the reflood portion of the transient as well. The results show that the test can be run successfully without exceeding facility limits.

TRAC-PF1/MOD1 IMPROVEMENT ACTIVITIES

We have either completed or are currently working on three areas of model improvement. First, we have added a CCFL model to the code. Thus far, we are seeing significant improvements in the comparison to tie-plate flooding data for the saturated injection case and the subcooled injection case. Second, we have added the capability to connect more than one PIPE or TEE to one VESSEL cell. Multiple-source connections provide a marked improvement in modeling capability, and was used successfully in the completion of the UPI test analyses. Finally, we are in the process of adding a separator model that will include liquid carryover and vapor carry under as a function of mixture flow rate and quality. The user will provide these data to the code in the input.

Future development activities will address improvements in the prediction of the core void distribution and entrainment during reflood, the prediction of direct contact condensation, and the prediction of deentrainment.

SUMMARY

We believe that Los Alamos is functioning as a vital participant within the 2D/3D Coordination Program. The results of our analytical efforts are being used to support test specification and design, improve understanding of phenomena occurring in tests, assess the predictive capability of TRAC, and identify needed areas of code improvement.

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TABLE 1
CCTF-UPI-ASSESSMENT SUMMARY

Run	Type	Results	
72 (C2-13)	No failure Symmetric Injection	Overall Agreement:	• Moderate
		Predicted:	• Multidimensional reflood • Negative core inlet flow • Location of liquid downflow • Rod temperatures
		Deficiencies:	• Excessive condensation of ECC fluid in cold legs • Overprediction of amount of downflow
76 (C2-16)	Single failure, single point injection	Overall Agreement:	• Moderate
		Predicted:	• Multidimensional reflood • Negative core inlet flow • Rod temperatures (average values)
		Deficiencies:	• Excessive condensation of ECC fluid in cold legs • Overprediction of amount of downflow • Location of downflow • Void distribution in core
78 (C2-18)	Single failure low power, stored energy	Overall Agreement:	• Moderate
		Predicted:	• Multidimensional reflood • Negative core inlet flow • Location of liquid downflow • Rod temperatures (average values)
		Deficiencies:	• Excessive condensation of ECC fluid in cold legs • Overprediction of amount of downflow • Void distribution in core
59 (C2-AS1)	Single failure, Asymmetric injection	Overall Agreement:	• Moderate
		Predicted:	• Multidimensional reflood • Negative core inlet flow • Rod temperatures (average values) • Amount of downflow
		Deficiencies:	• Excessive condensation of ECC fluid in cold legs • Exact location of downflow

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TABLE 1 (cont)
CCTF-UPI-ASSESSMENT SUMMARY

Run	Type	Results	
57 (C2-AA1)	80% single failure, asymmetric injection, high power, stored energy	Overall Agreement:	• Insufficient
		Predicted:	• Multi dimensional reflood
		Deficiencies:	• Overprediction liquid entrainment into hot legs and steam binding • Overprediction of amount of liquid downflow • Location of liquid downflow • Core water accumulation

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REFERENCES

Note: Reports in the LA-2D/3D-TN series contain proprietary data from the Japan Atomic Energy Research Institute (JAERI), which cannot be released to a third party without authorization from the US NRC and JAERI.

1. Safety Code Development Group, "TRAC-PD2: An Advanced Best-Estimate Computer Program for Pressurized Water Reactor Loss-of-Coolant Accident Analysis," Los Alamos National Laboratory report LA-8709-MS, NUREG/CR-2054 (April 1981).
2. F. Motley, "Research Information Report Results From TRAC Analysis of Cylindrical Core Test Facility Core I Test Series," Los Alamos National Laboratory document LA-2D/3D-TN-86-10 (July 1986).
3. M. W. Cappiello, H. J. Stumpf and B. E. Boyack, "CCTF Core II UPI Summary," Los Alamos National Laboratory document LA-2D/3D-TN-86-16 (to be issued).
4. Safety Code Development Group, "TRAC-PF1/MOD1: An Advanced Best-Estimate Computer Program for Pressurized Water Reactor Thermal-Hydraulic Analysis," Los Alamos National Laboratory report LA-10157-MS, NUREG/CR-3858 (July 1986).
5. M. Cappiello, "CCTF Run 59 TRAC-PF1/MOD1 Analysis," Los Alamos National Laboratory document LA-2D/3D-TN-85-1 (January 1985).
6. M. W. Cappiello, "TRAC-PF1/MOD1 Analysis of CCTF No-Failure UPI Test C2-13 (Run 72)," Los Alamos National Laboratory document LA-2D/3D-TN-86-7 (July 1986).
7. H. J. Stumpf, "CCTF Run 76 TRAC-PF1/MOD1 Analysis," Los Alamos National Laboratory document LA-2D/3D-TN-86-6 (to be issued).
8. H. J. Stumpf, "CCTF Run 78 TRAC-PF1/MOD1 Analysis," Los Alamos National Laboratory document LA-2D/3D-TN-86-5 (to be issued).
9. B. E. Boyack, "TRAC-PF1/MOD1 Analysis of CCTF UPI Test C2-AA1 (Run 57)," Los Alamos National Laboratory document LA-2D/3D-TN-86-11 (to be issued).
10. H. J. Stumpf, "CCTF Run 71 TRAC-PF1/MOD1 Analysis," Los Alamos National Laboratory document LA-2D/3D-TN-86-8 (to be issued).
11. K. A. Williams, "Research Information Report on the Experimental and Analytical Results of the Core-I Test Series at the Japan Atomic Energy Research Institute Slab Core Test Facility," Los Alamos National Laboratory document (to be issued).
12. P. R. Shire and B. E. Boyack, "TRAC Analysis of Upper Plenum Thermal-Hydraulic Phenomena in the Slab Core Test Facility," to be published in the Transactions of the ANS, 1986 Winter Meeting, Washington, D. C., November 16-20, 1986.
13. J. C. Lin, "TRAC-PF1/MOD-1 Calculation of SCTF Core-II Test S2-SH2 (Run 605)," Los Alamos National Laboratory document LA-2D/3D-TN-85-15 (March 1985).

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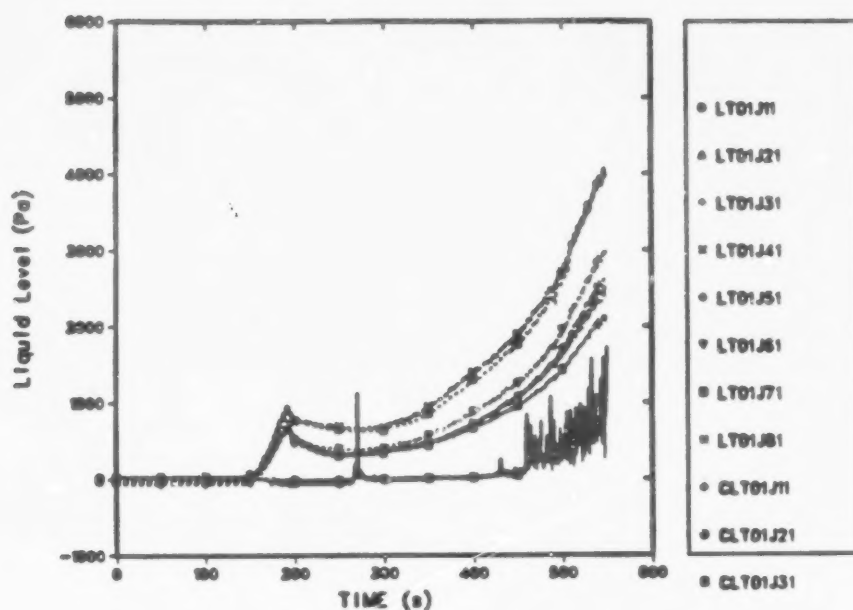


Fig. 1.

Upper-plenum liquid level calculated for SCTF Run 605 using the full-facility model.

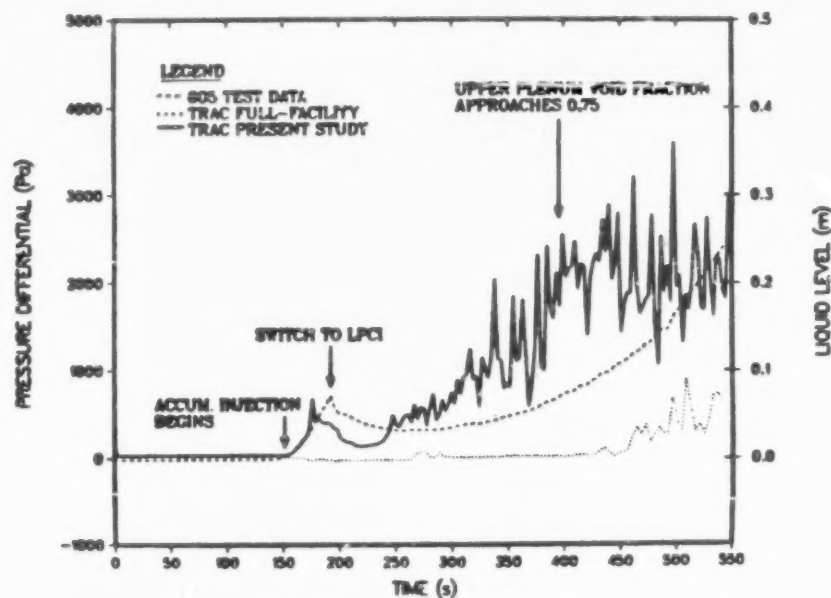


Fig. 2.

Upper-plenum liquid level calculated for SCTF Run 605 using the stand-alone upper plenum model.

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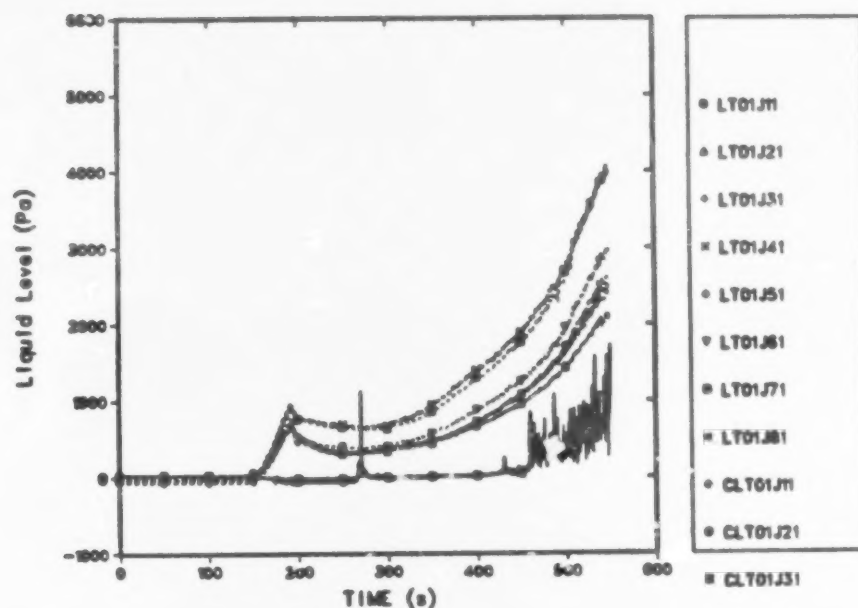


Fig. 1.

Upper-plenum liquid level calculated for SCTF Run 605 using the full-facility model.

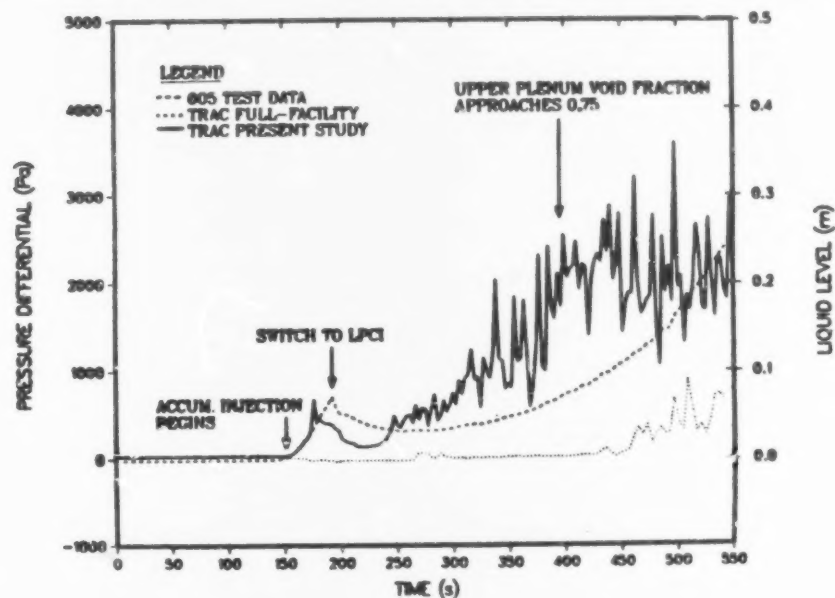


Fig. 2.

Upper-plenum liquid level calculated for SCTF Run 605 using the stand-alone upper plenum model.

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MULTIDIMENSIONAL REPRESENTATION OF GPWR

PRIMARY SYSTEM IN 200 % BREAK LOCA CALCULATION

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Introduction

The 3D-Vessel capability of TRAC-PF1/Mod1 (12.5) was used to study the influence of combined cold and hot leg ECC injection on flow and temperature distributions in the upper plenum as well as on the water break through from the upper plenum to the core region and finally on the core cooling.

2 LOCA calculations have been performed. Based on a German Pressurized Water Reactor (GPWR) system a TRAC-PF1 input deck was designed to simulate

- the pressure vessel by a three-dimensional component of 15 axial levels, 4 radial rings and 8 azimuthal segments
(core - 2 rings, core baffle - 1 ring, downcomer - 1 ring)
- the 4 loops separately by one - dimensional components including a fine noding of the steam generators and the main coolant pumps
- the complete ECC-injection system (cold and hot leg injection) consisting of high and low pressure pumps (HP/LP) and accumulators (ACCU).

The same nodalization (about 1160 computational cells) was used in both calculations (see figures 1, 2).

Paper prepared for the
14th Water Reactor Safety Information Meeting
October 27-31, 1986, Gaithersburg, Maryland

The basis of the calculation is lined out in figures 3, 4 and 5.

The only differences between the two runs are:

Calculation 1:

- HP/LP/ACCU injection into hot legs of loop 1 and loop 2 (pressurizer loop),
- 100 % power.

Calculation 2:

- HP/LP/ACCU injection into hot legs of loop 1 and loop 3,
- 106 % power, decay heat.

Discussion of selected results

As can be seen in figure 6 the core average pressure histories are identical up to 25 sec of the transient. At this time more steam is produced in calculation 1 due to the fact that the precooling of cell 15 by the pressurizer discharge flow is now supported by the accumulator injected water into loop 2 hot leg. The pressurizer empties at about 17 sec in both cases. In the later time only steam is flowing from the pressurizer into loop 2. At about 21 sec the accumulators start to inject subcooled water ($\delta_{ECC} \sim 30^\circ\text{C}$) into the hot legs and cold legs (see figures 2, 3). The LP systems finally are started at 37 sec.

A subsequent result can be observed from figure 7, where the vessel liquid volume fractions are plotted as a function of time. At about 21 sec the core region dries out, however 1-2 seconds later when loop 2 hot leg accumulator flow becomes effective the core liquid volume fraction increases. During the transient from 50-65 seconds the core liquid volume fractions develop differently, especially when the lower plenum is filled up again at 53 sec in both cases and the reflooding of the core begins.

The integrated liquid massflow rates for the various cells are shown in figures 8 and 9.

In calculation 1 cells 15 and 16 show excessive water break through at the tie plate. In calculation 2 cells 16 and 12 indicate water break

through. These cells are located next to the hot legs in which ECC-water is injected. Associated with the liquid downflow a substantial subcooling of the liquid up to 70 K (see figures 10, 11) can be observed.

In figure 12 the cladding temperatures of the average rod in cell 15 at different elevations is plotted as a function of time.

As mentioned above the discharge flow of the pressurizer results in a precooling of the average rod in cell 15. However, after 20 sec a reheating can be observed.

In case 1 the hot leg ECC injection (loop 2) causes a sudden turn around of the temperature rise and an effective cool down of the average rod. In case 2 the reheating process continues according to the heat transfer rates based on the recent flow conditions during this period. In figure 13 the cladding temperature histories of the average rod in cell 4 are plotted. The thermohydraulics in cell 4 is dominated by steam upflow (or high void two phase flow).

The envelope of the maximum average cladding temperatures in cells 1 to 16 (see figure 14) is quite similar in both cases up to 55 s after break initiation neglecting the small difference in the temperature values of the 2. calculation which may come from the initial 6 % overpower and the decay heat assumption (figure 5). A major difference after 55 s indicates that the thermohydraulic behaviour in the core region changed significantly. An illustration of the cooling front propagation in radial and azimuthal direction is given in figures 15 to 17. At certain times of the transient the quenched core regions have been evaluated. Shaded areas mean that the average rod has already been quenched completely in this area (volume).

At 55 s in calculation 1 cells 7, 8, 9, 14, 15, 16 have been quenched. In this area all the ECC water, which is injected into loop 1 and 2 drain down through the core to the lower plenum.

The 2. calculation shows at 55 s a different result. Only 3 core areas (cells 11, 12, 16) are quenched completely. However, these cells are facing those hot legs in which the ECC water is injected - loop 1 and loop 3.

The situation which is qualitatively similar in both cases, can be described as establishing of flow channels. Once the channels are established they stay established through out the transient.

This is in excellent agreement with first experimental results from the UPTF-test 12 /1/.

In this test steam is injected below the tie plate via the core simulator (about 250 kg/s total) and ECC water is injected into the hot legs according to the individual cases (800 kg/s total).

The dark squares in figures 18 and 19 mark the location of the break-through detectors which indicate water flowing down to the core region at this location.

It clearly can be observed that the water break through areas are established in the vicinity of the hot leg nozzles through which ECC water enters the upper plenum.

Summary

- Based on a TRAC-PF1/Mod1 3D-nodalization of the GPWR vessel the calculational results show break through of subcooled ECC water at the tie plate in areas located below those hot leg nozzles through which ECC water is injected into the upper plenum.
- When break through is started in these areas, the ECC water is drained down via the core to the lower plenum. Steam (two phase mixture) is flowing up from the core to the upper plenum in adjacent areas.
- The location and the area of the 'flow channels' (water down and steam up) is coupled to the activated hot leg ECC injection, the location of the broken hot leg and the pressurizer discharge connection.
- The fluid temperature distribution in the upper plenum and core obviously is strongly dependent on the formation of the flow channels. Fluid temperatures ranges in
 - break through channels from T_{sat} to $\Delta T_{\text{sub}} \sim 70 \text{ K}$ and in

- steam upflow channels from T_{sat} to some superheating of $\Delta T_{\text{sub}} \sim 5 \text{ K}$
- First experimental results from a UPTF separate effects test (tie plate CCF, subcooled) confirm the establishing of flow channels between upper plenum and core for water down and steam upflow.

A conclusive remark may be made:

Experimental data show clearly the flow channelling in the case of hot leg ECC injection and steam injection in the core region.

Using the 3D vessel capability of TRAC-PF1/Mod1 the calculational results also show a flow channelling, although the global data do not differ significantly. Therefore a multichannel simulation of a GPWR-vessel may be an adequate alternative in large break LOCA analyses.

Reference

- /1/ Upper Plenum Test Facility - UPTF - Test No. 12, tie plate countercurrent flow test, R515 /86/, Kraftwerk Union AG (KWU), Erlangen, August 1986

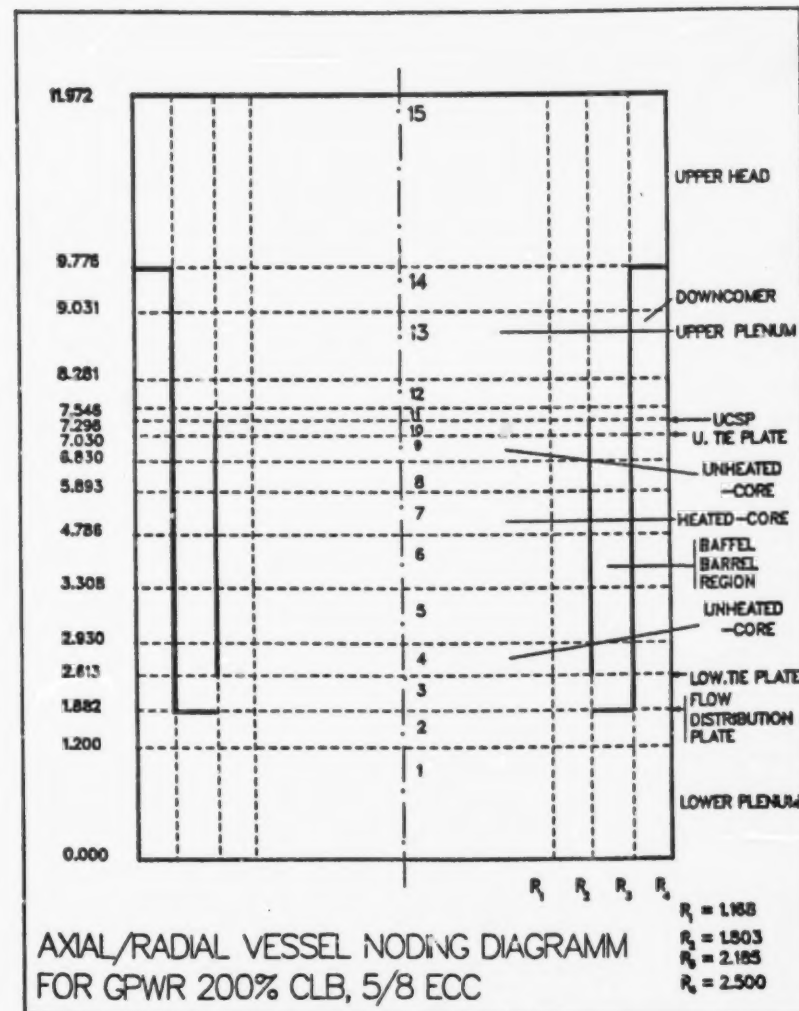
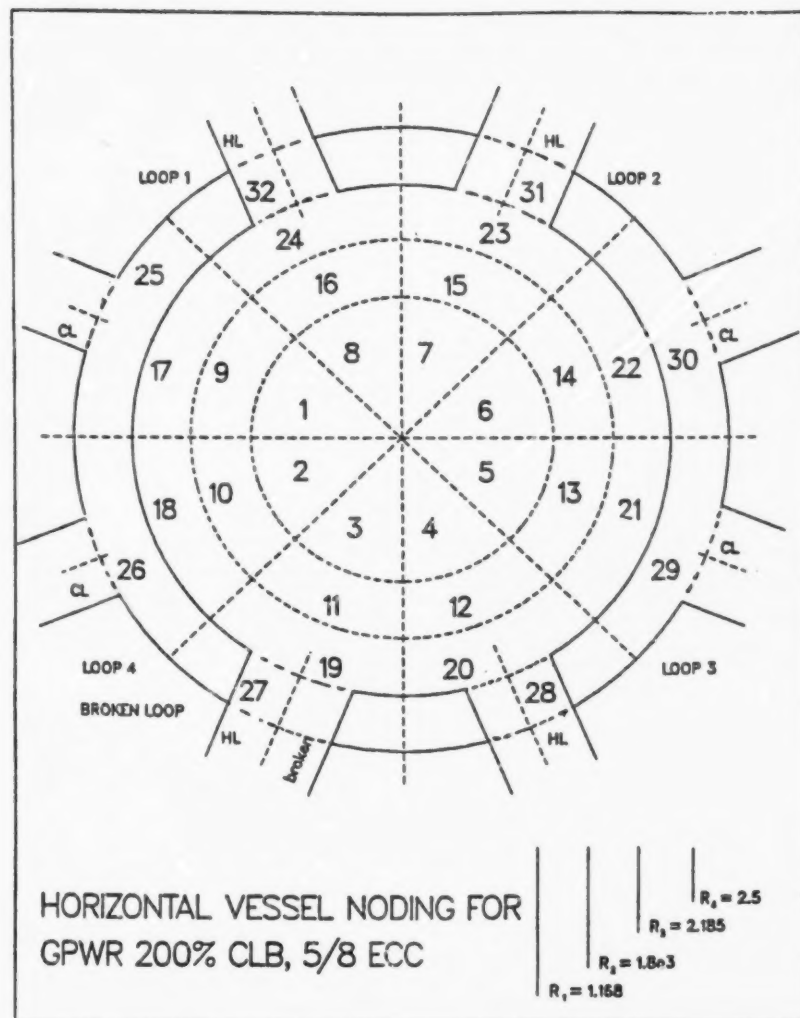


figure 1

GPWR - LOOP 2 (PRESSURIZER LOOP)

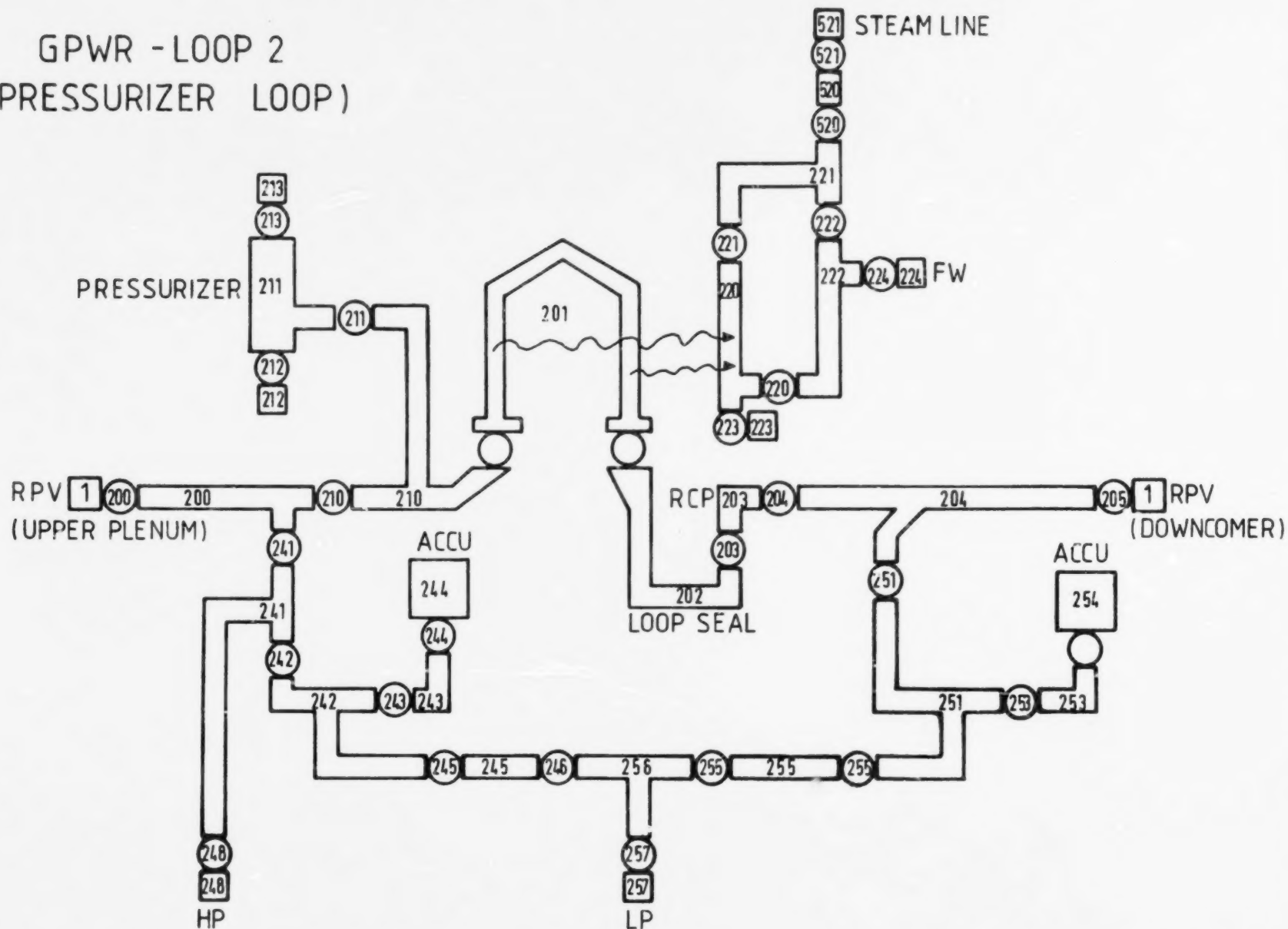


figure 2

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Basis of calculations

- GPWR 1300 MWe, KWU design, combined cold and hot leg ECC injection
- 200 % cold leg break
- ECC injection system activated

	Calculation #1		Calculation #2	
	HL	CL	HL	CL
Loop 1	A,LP,HP	A,LP	A,LP,HP	A,LP
Loop 2 (pressurizer)	A,LP,HP	A,LP	---	A,LP
Loop 3	---	A,LP	A,LP,HP	A,LP
Loop 4 (broken loop)	-,LP,HP	---	-,LP,HP	---

HL: Hot leg

CL: Cold leg

A : Accumulator

LP: Low pressure pump

HP: High pressure pump

figure 3

Basis of Calculations (cont.)

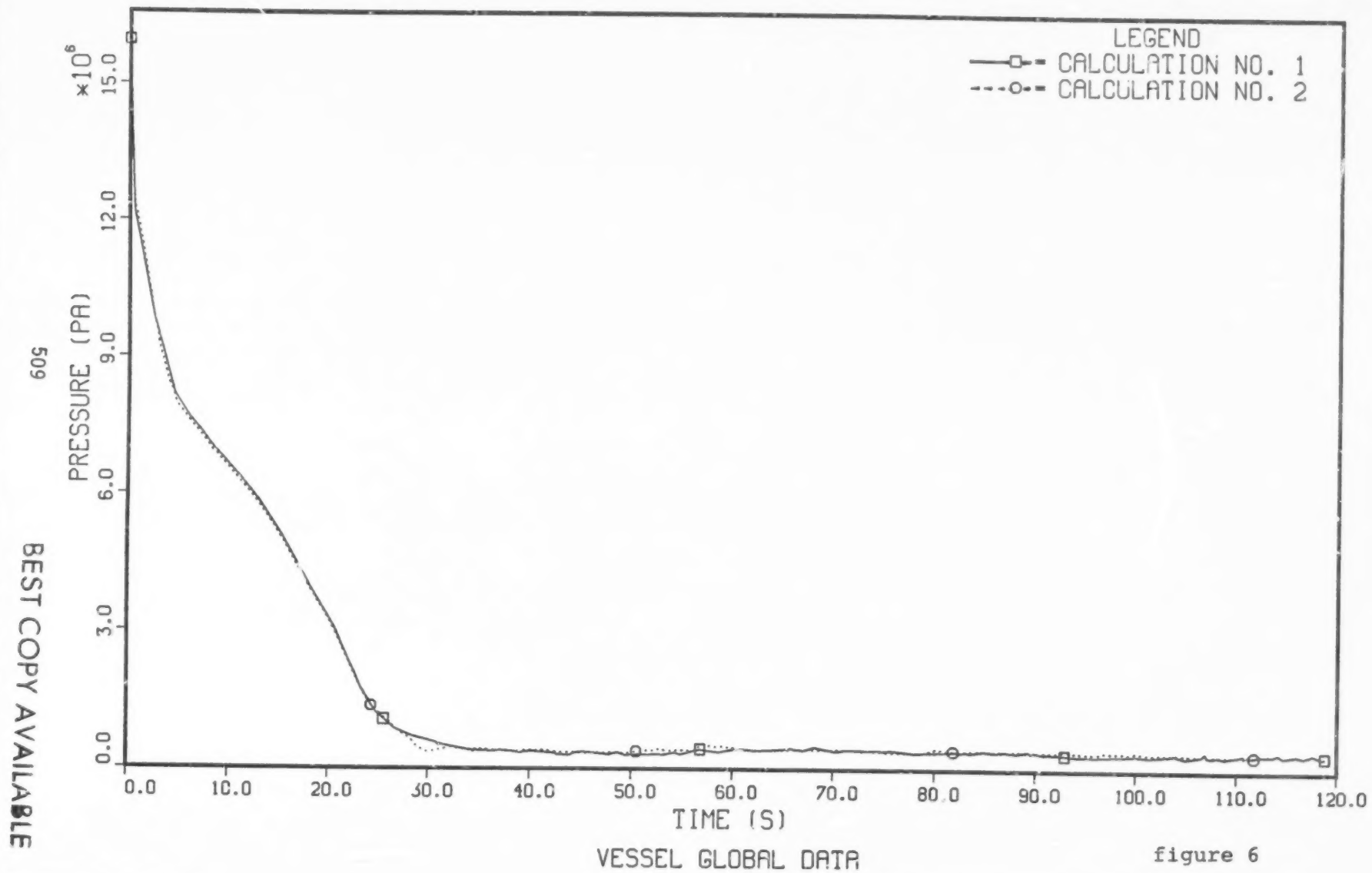
Initial Conditions

- Initial power
 - . Calculation #1 : 100%
 - . Calculation #2 : 106%
- Maximum linear power avg. rods
 - . Calculation #1 : 271 W/cm
 - . Calculation #2 : 287 W/cm
- Power distribution
 - . axial : chopped cosinus
 - . radial inner ring (81 FA) : 1.0745
 - . outer ring (112 FA) : 0.9461
- Gap heat transfer coefficient
at 207 W/cm : 6100 W/m²K
- Average burnup : 14 MWd/kgU

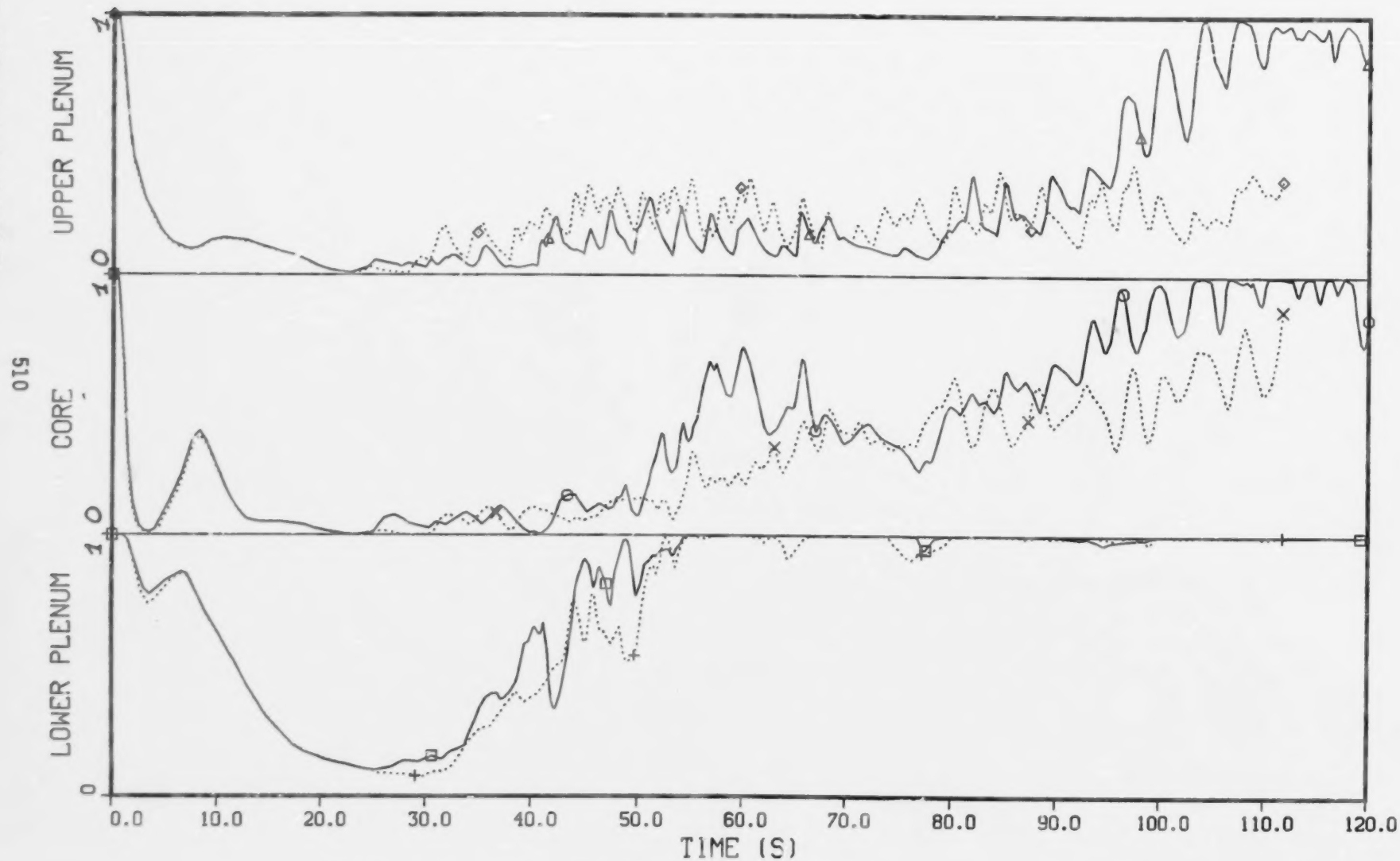
Basis of Calculations (cont.) Boundary Conditions

- Decay heat
 - . Calculation #1 : DIN
 - . Calculation #2 : $DIN + 2\sigma$
- Start of HP - injection : 33s
- Start of LP - injection : 38s
- Switch off of RCPS automaticaly at 0.5s

GPWR 200% CLB, 5/8 ECC



GPWR 200% CLB, 5/8 ECC

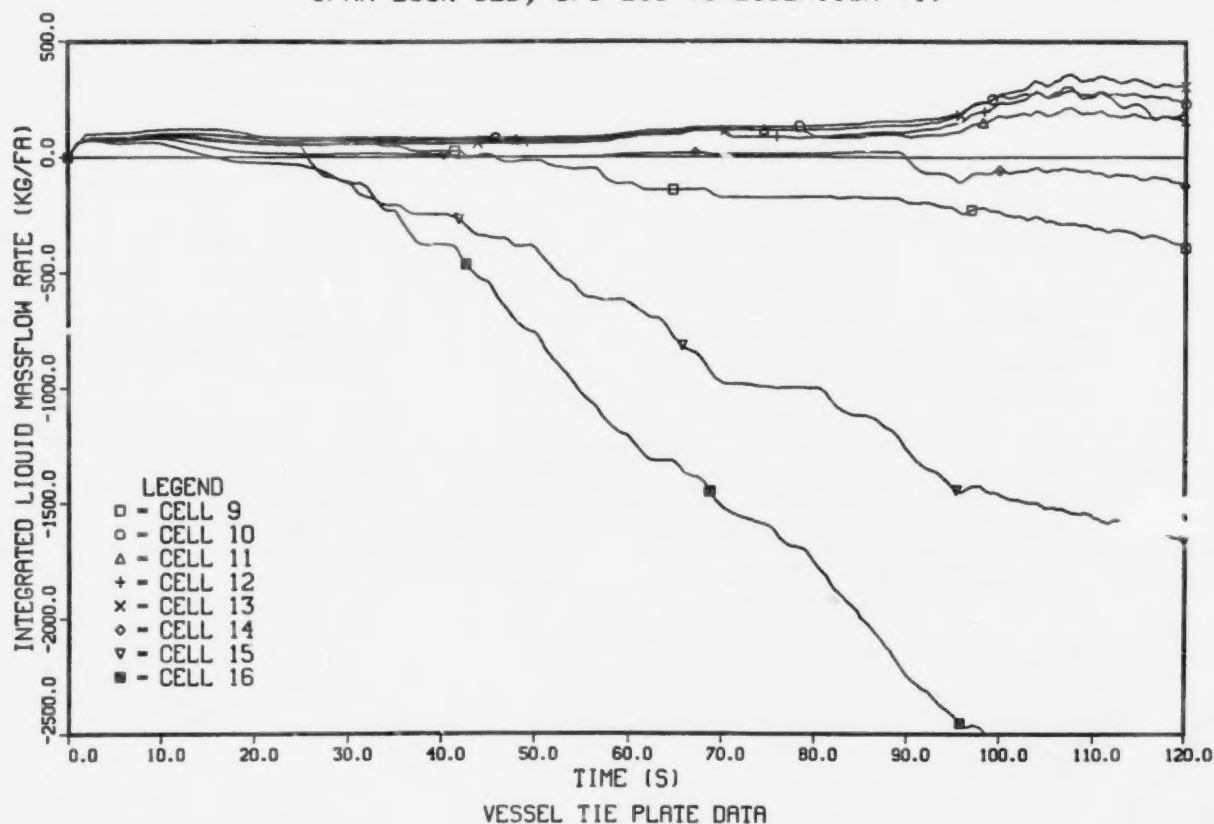


VESSEL LIQ. VOL. FRACTION (— CALC. #1, ----- CALC. #2)

figure 7

510

GPWR 200% CLB, 5/8 ECC (CALCULATION #1)



GPWR 200% CLB, 5/8 ECC (CALCULATION #2)

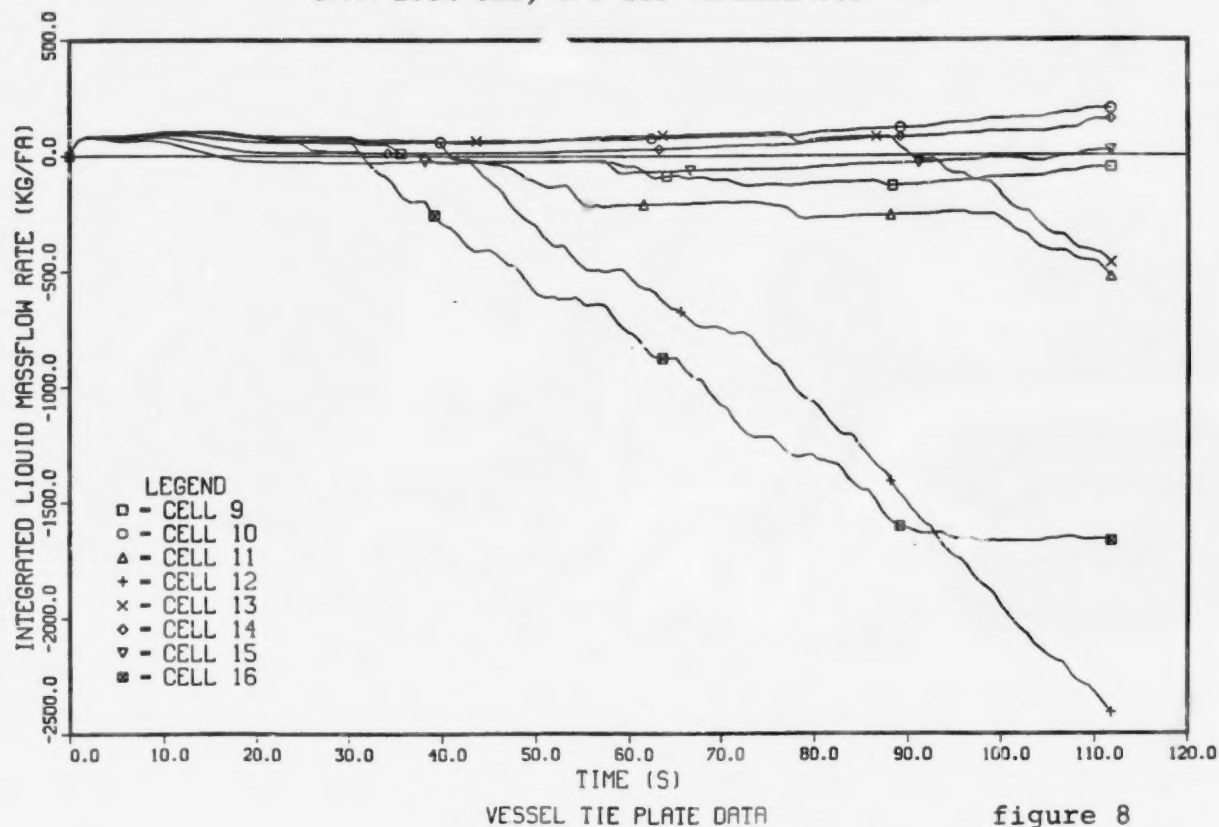
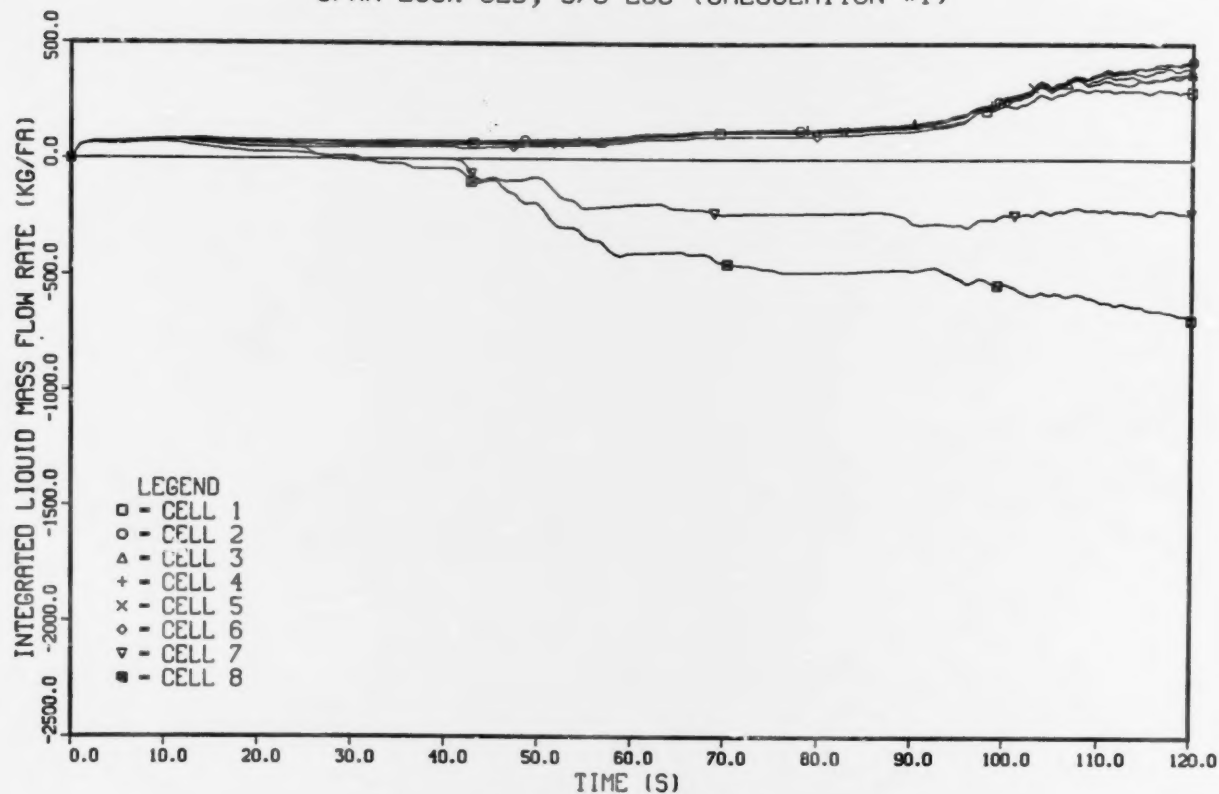


figure 8

GPWR 200% CLB, 5/8 ECC (CALCULATION #1)



GPWR 200% CLB, 5/8 ECC (CALCULATION #2)

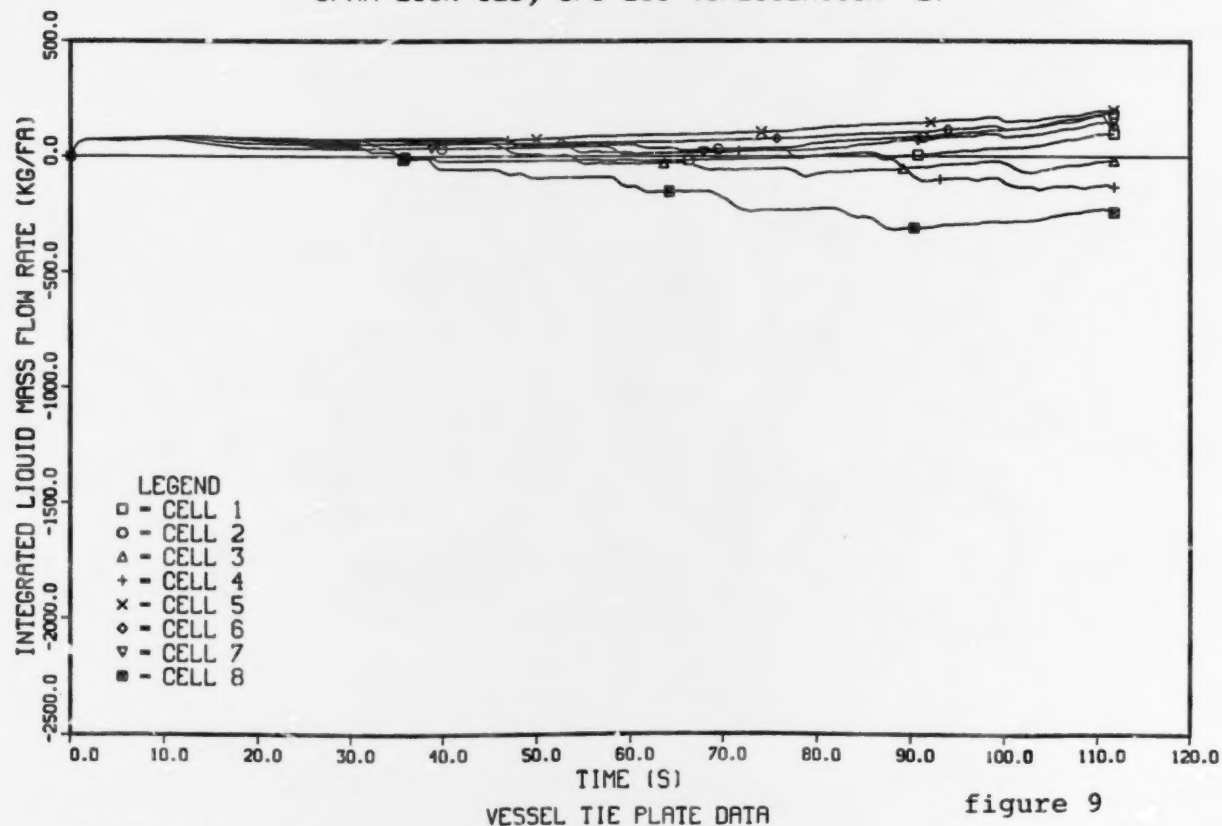
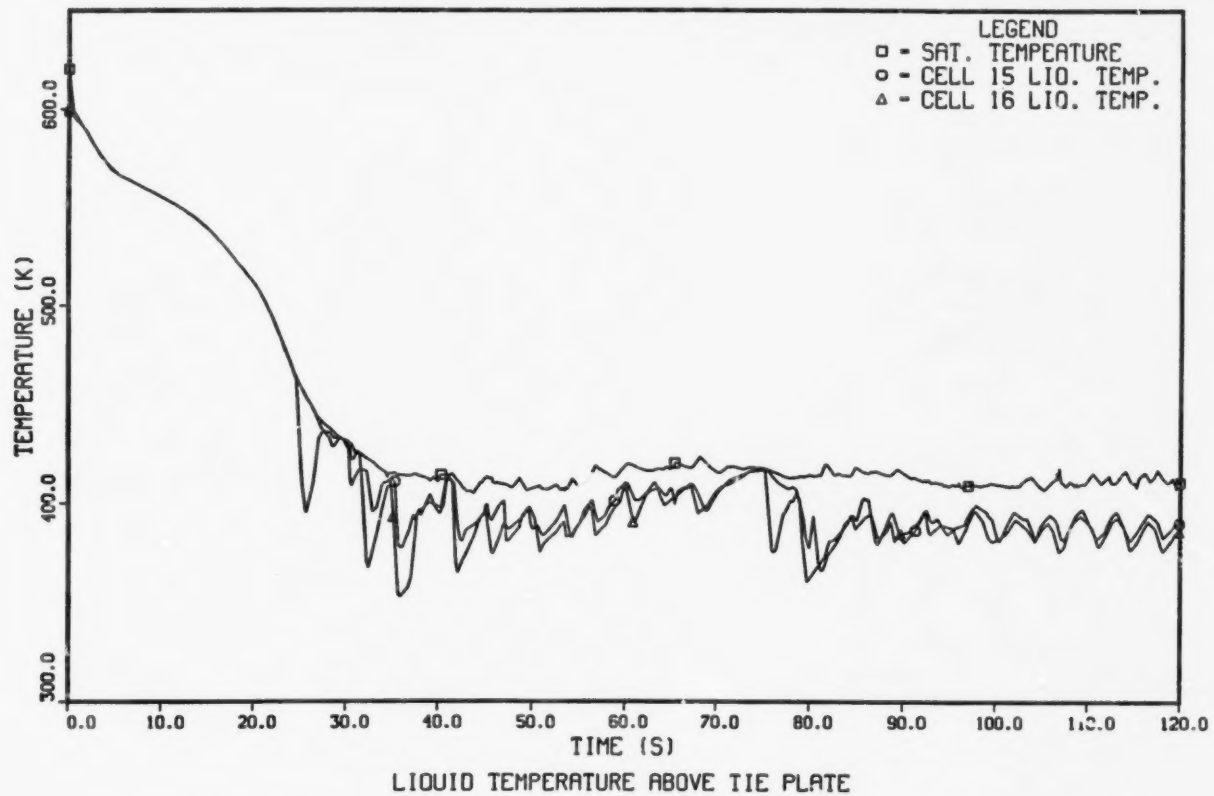


figure 9

GPWR 200% CLB, 5/8 ECC (CALCULATION #1)



GPWR 200% CLB, 5/8 ECC (CALCULATION #2)

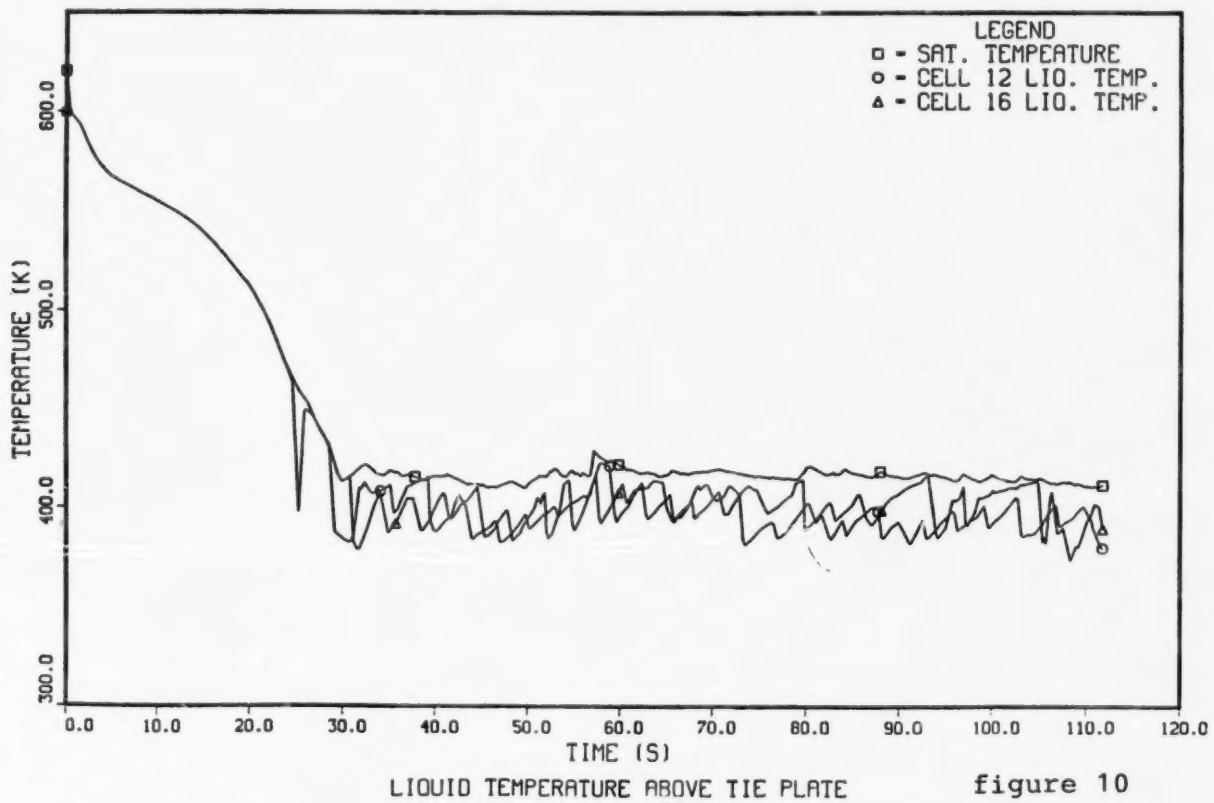
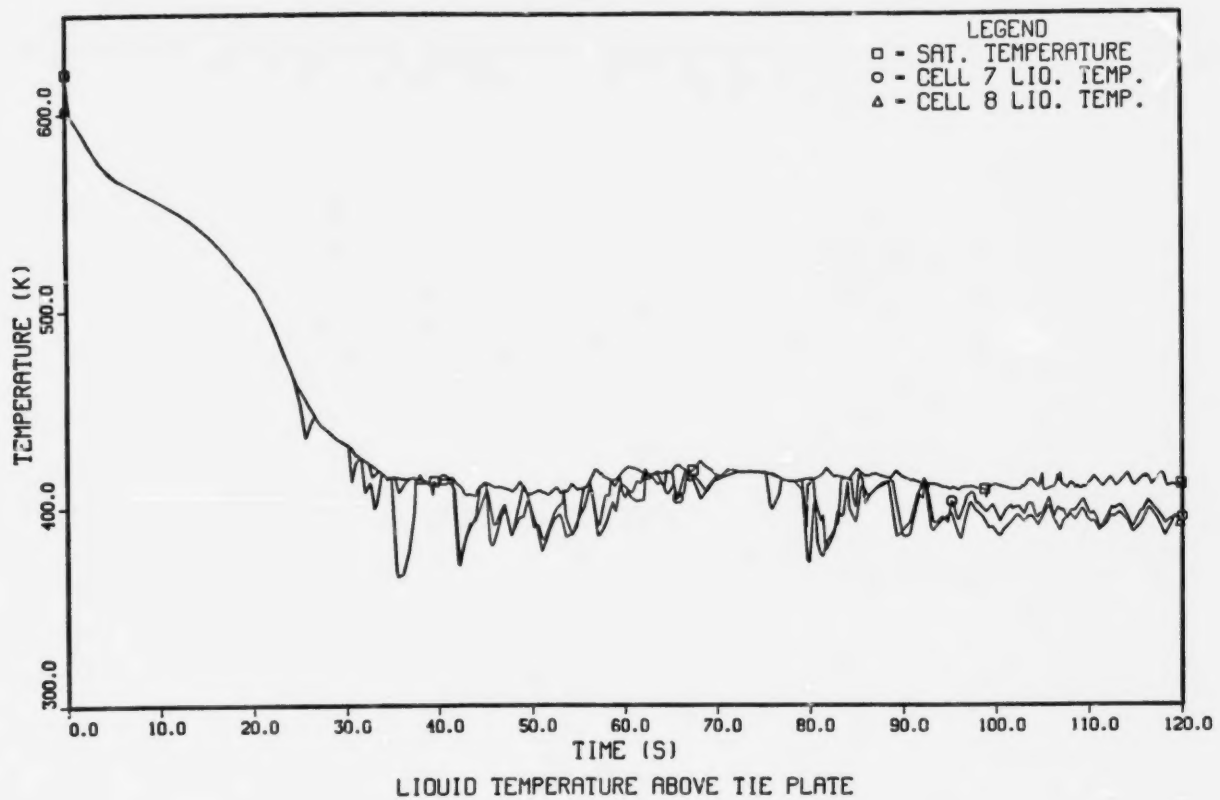


figure 10

GPWR 200% CLB, 5/8 ECC (CALCULATION #1)



GPWR 200% CLB, 5/8 ECC (CALCULATION #2)

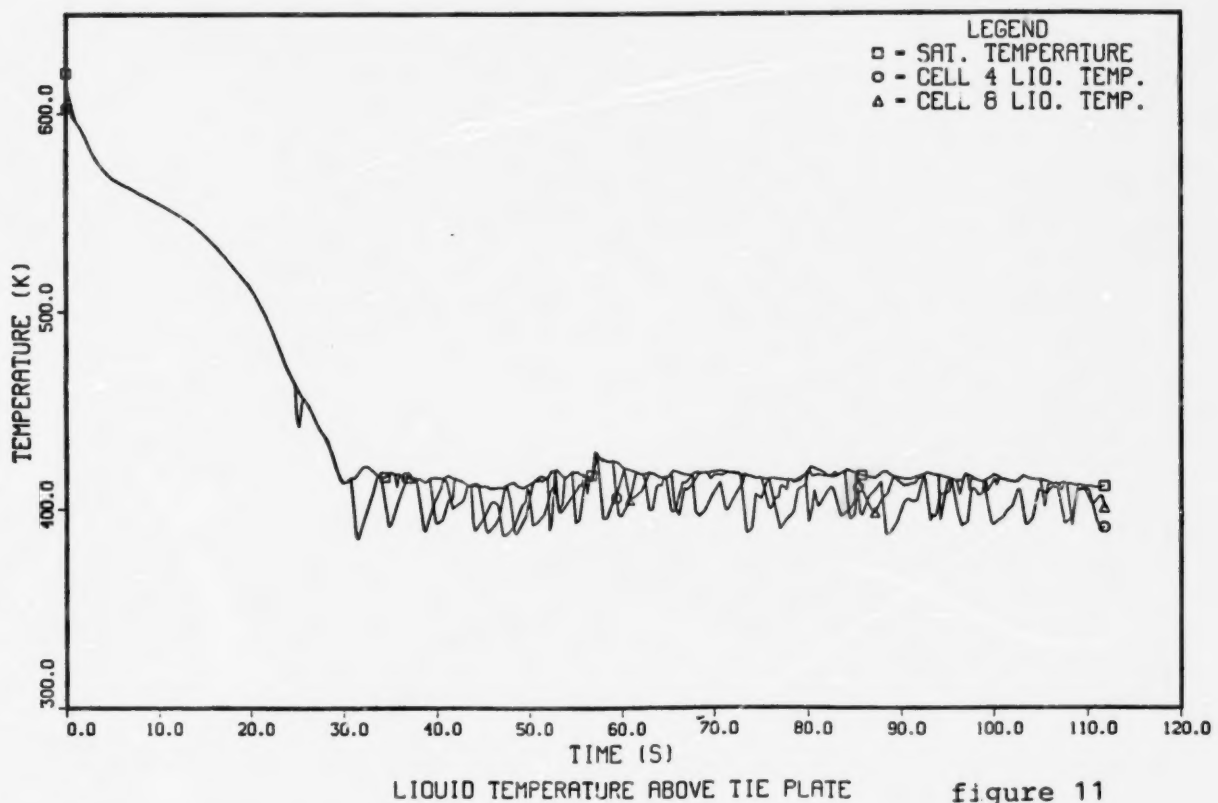
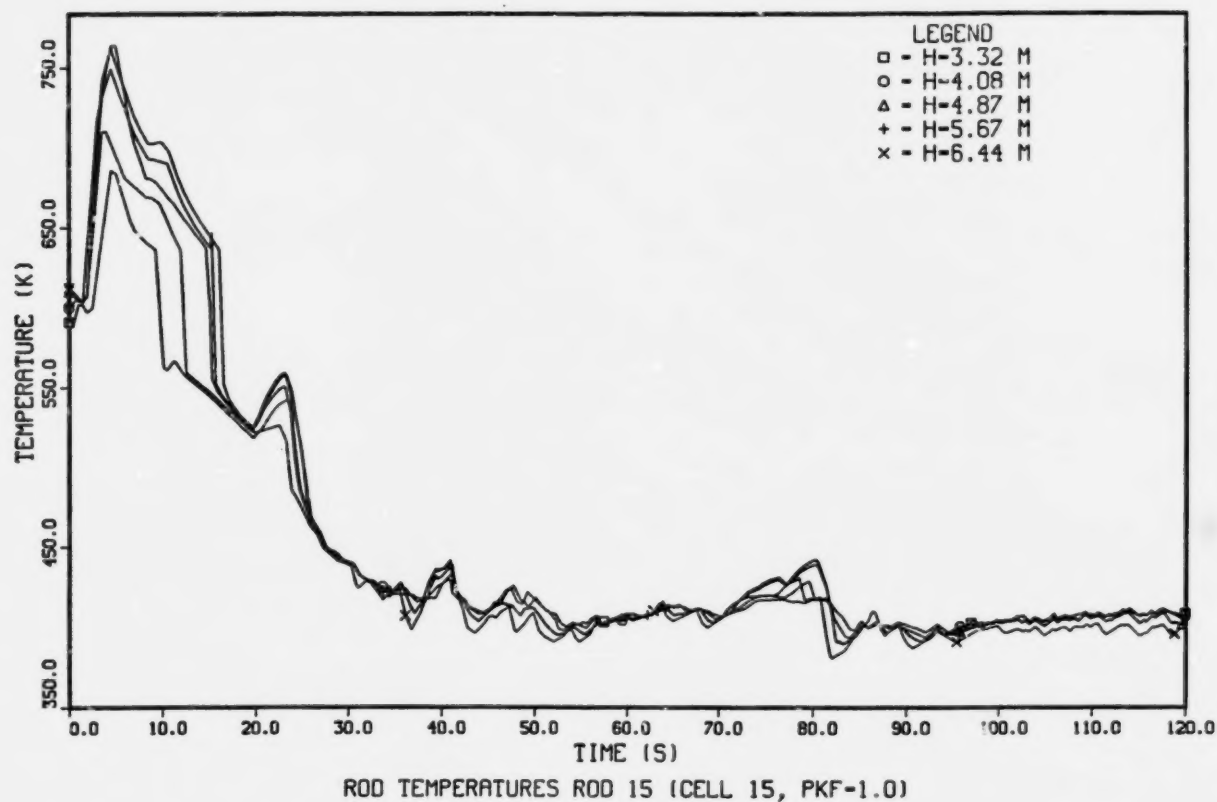
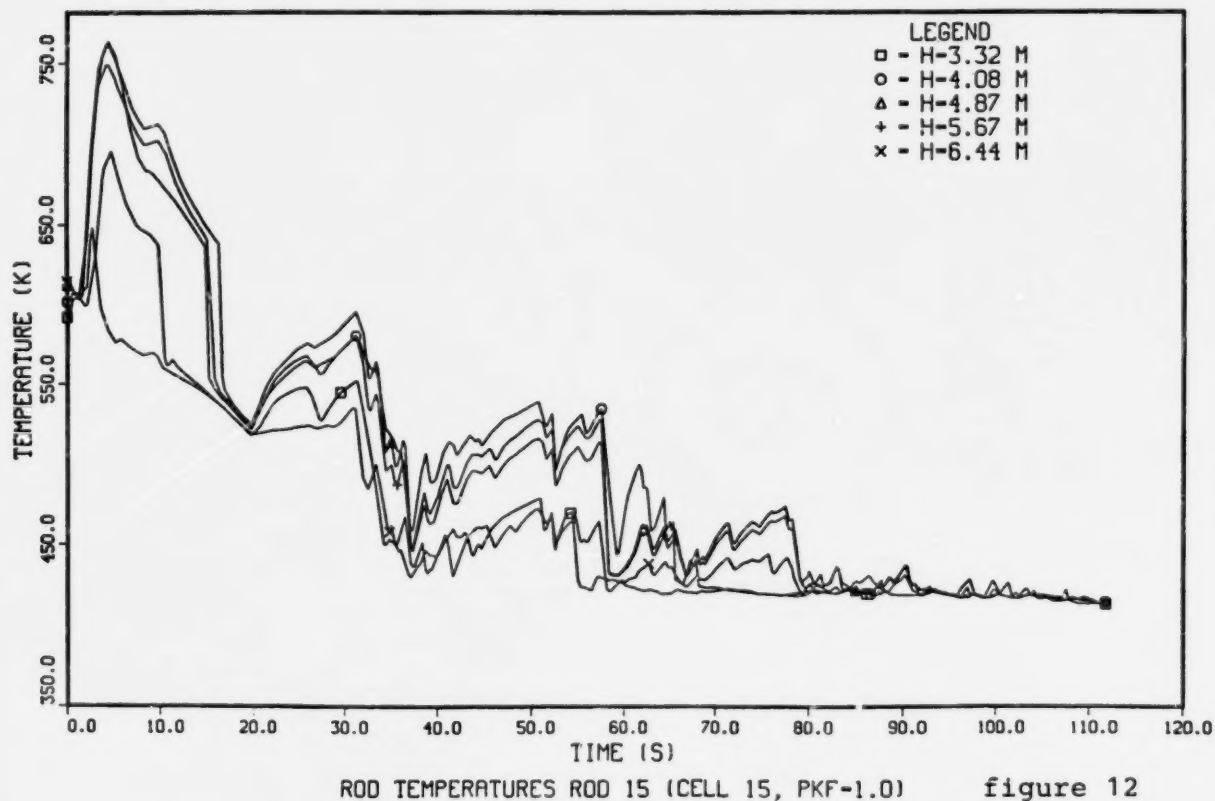


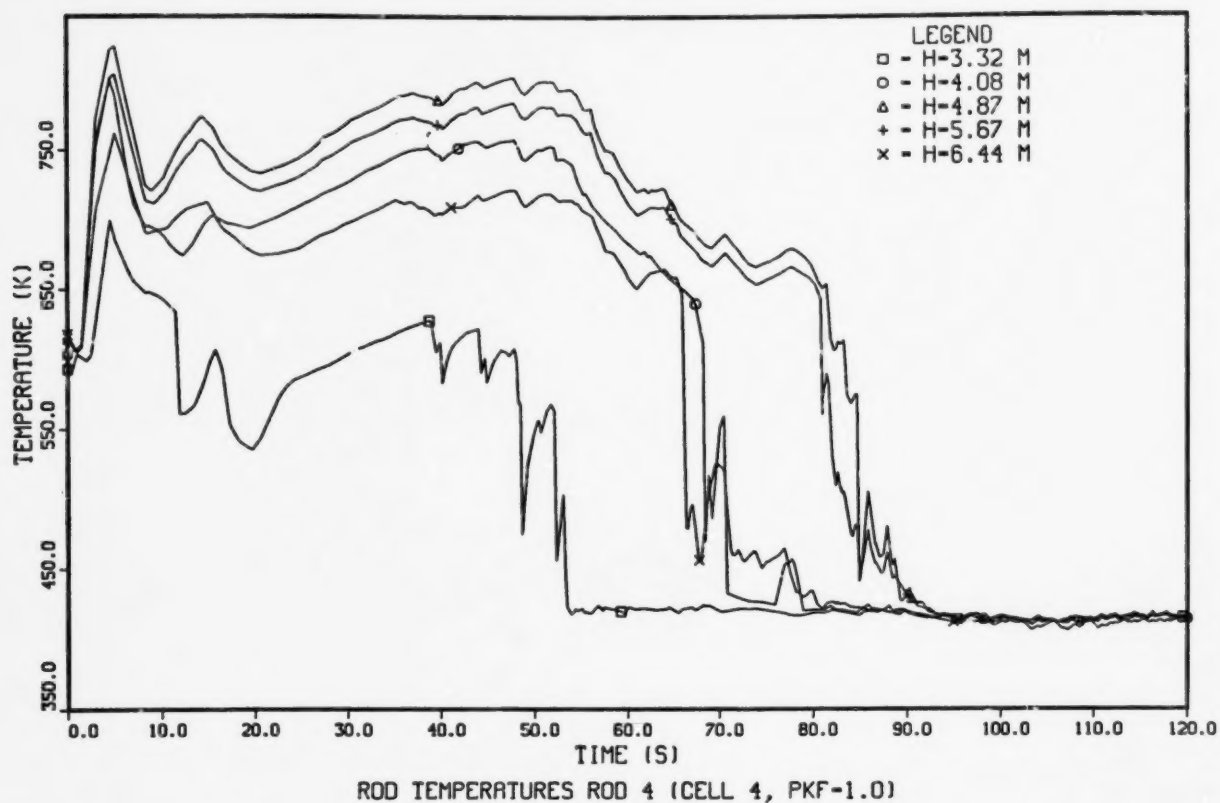
figure 11

GPWR 200% CLB, 5/8 ECC (CALCULATION #1)



GPWR 200% CLB, 5/8 ECC (CALCULATION #2)





GPWR 200% CLB, 5/8 ECC (CALCULATION #2)

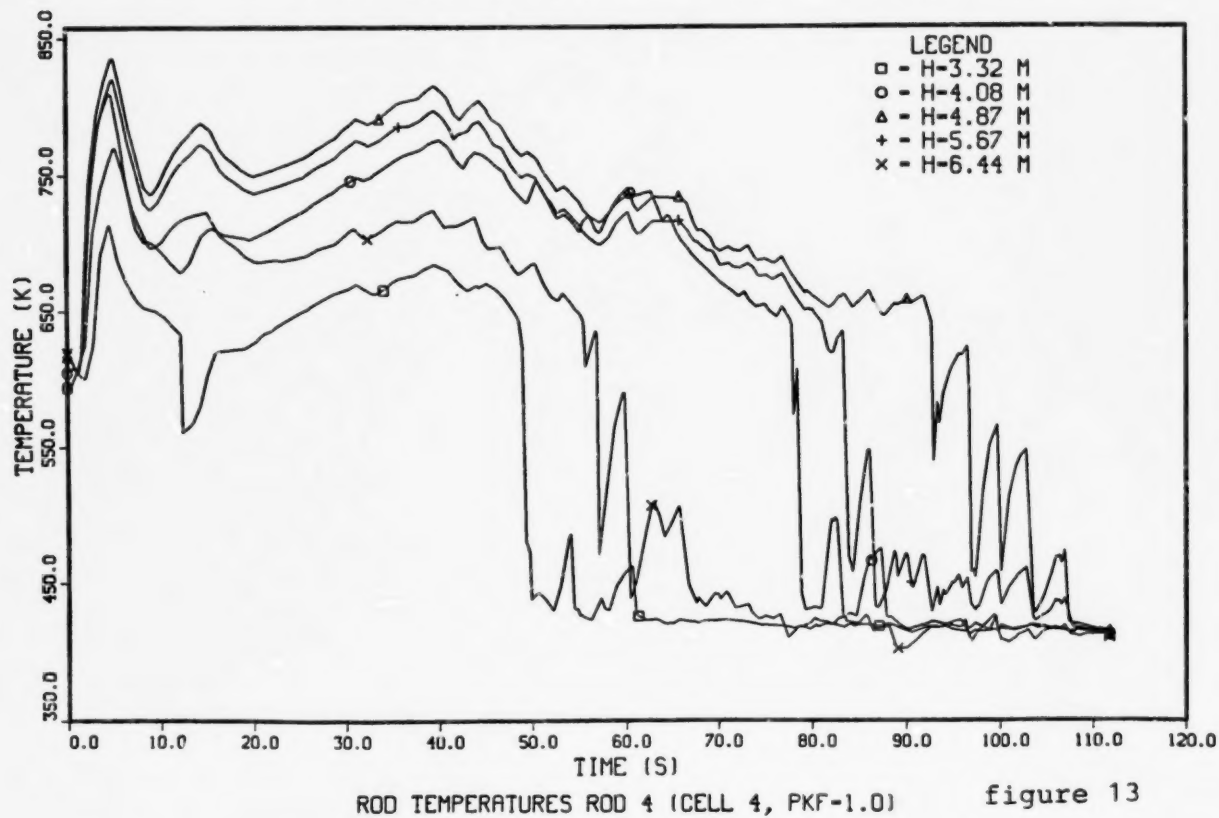
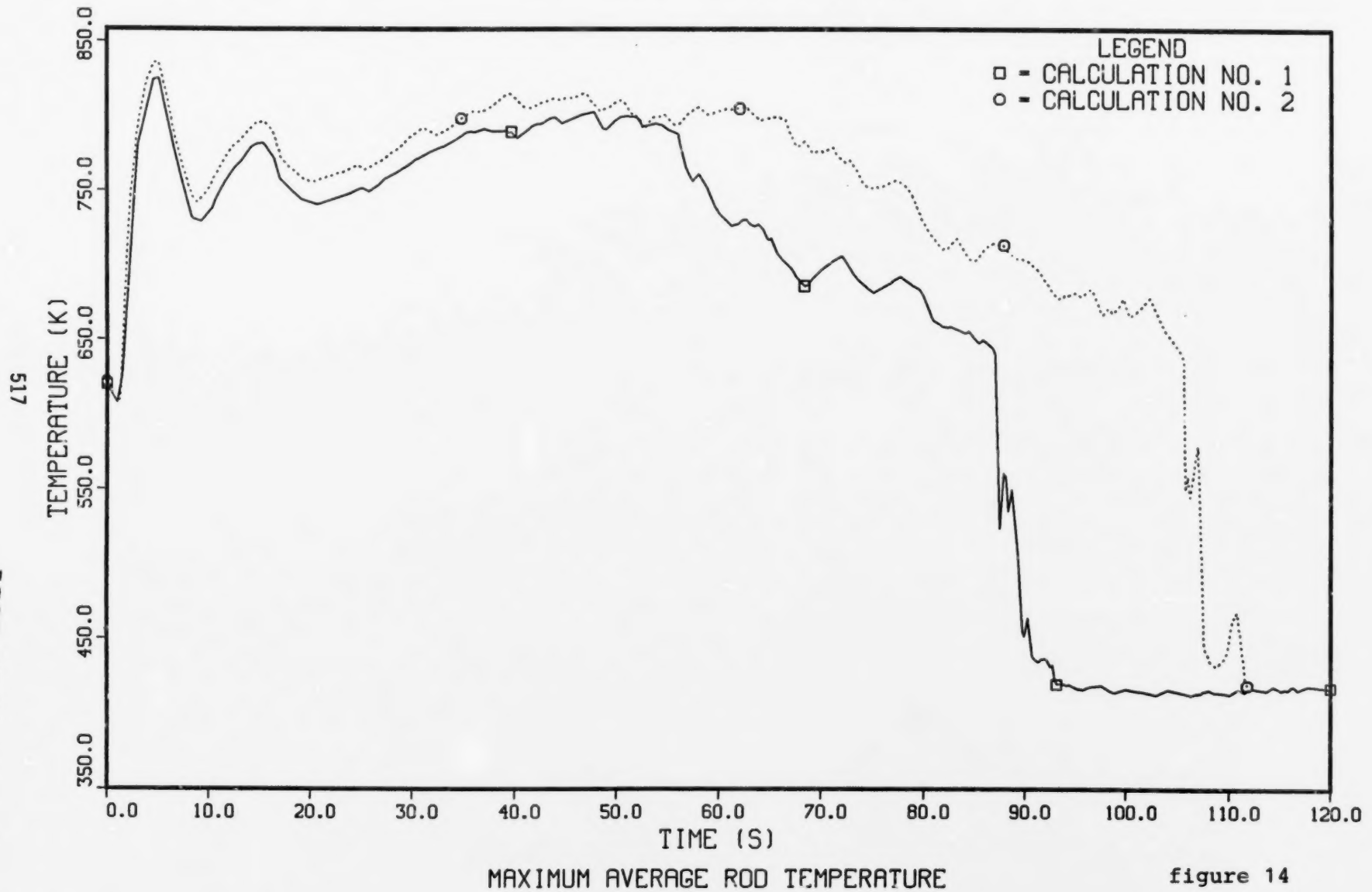


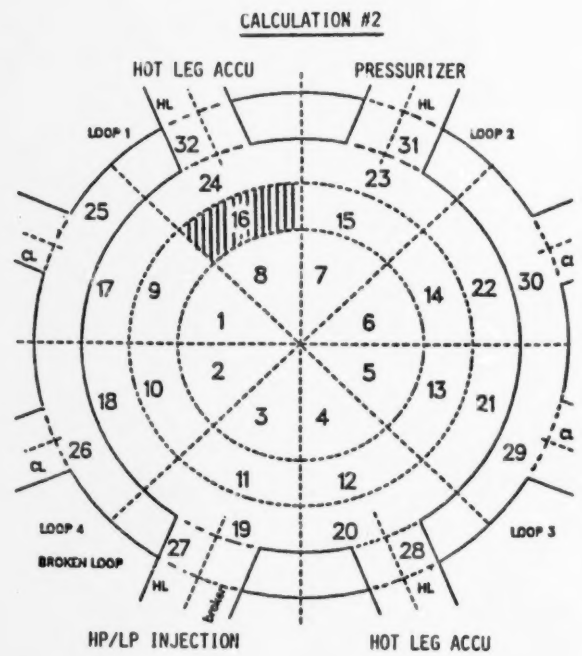
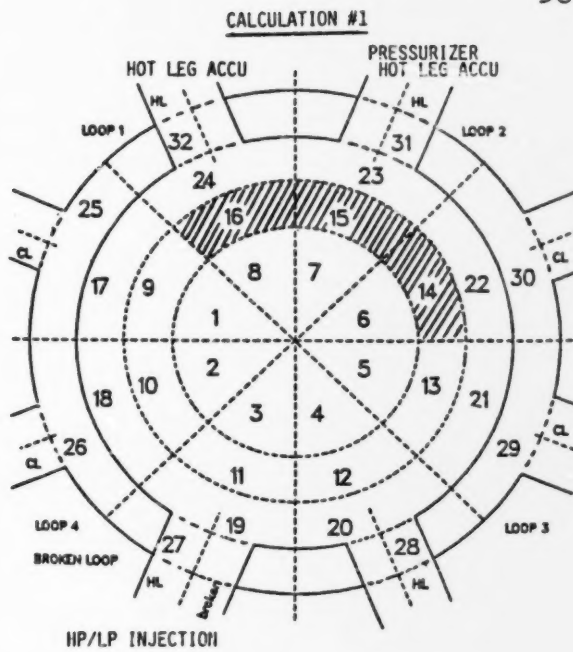
figure 13

GPWR 200% CLB, 5/8 ECC

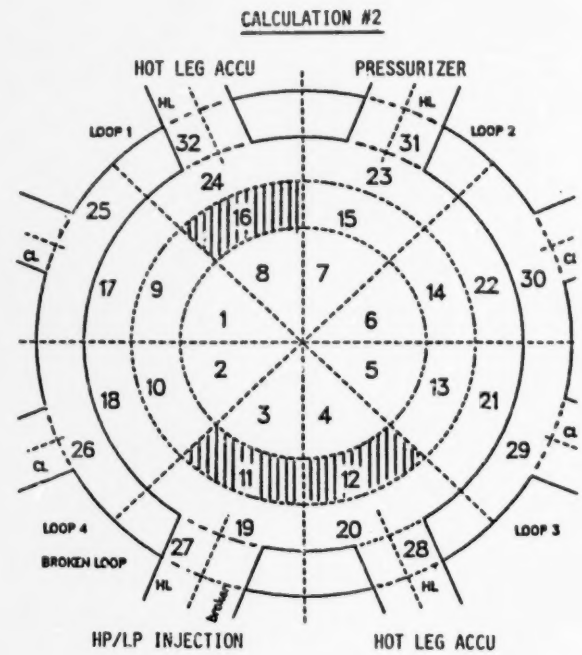
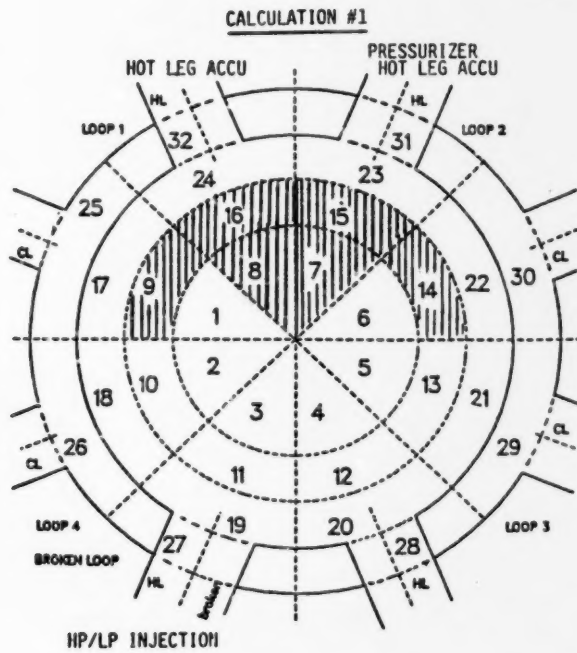


QUENCHED CORE REGIONS

38 s



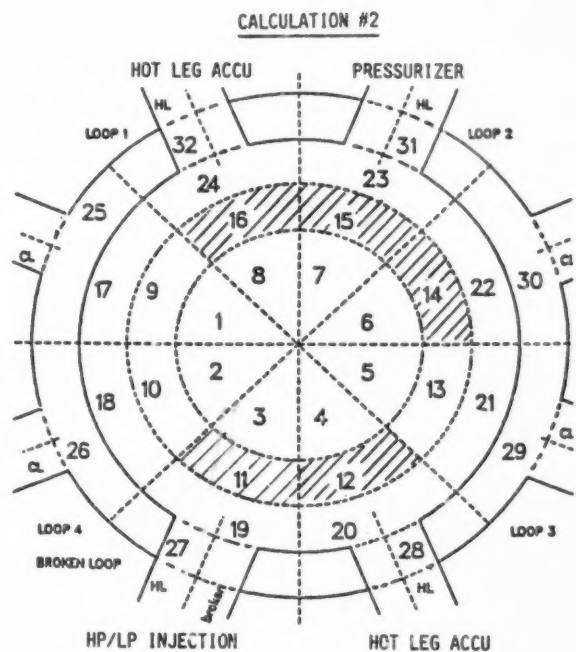
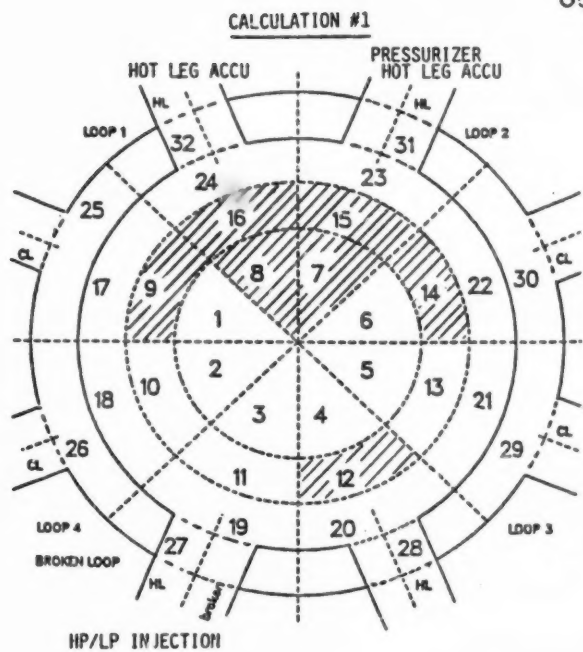
55 s



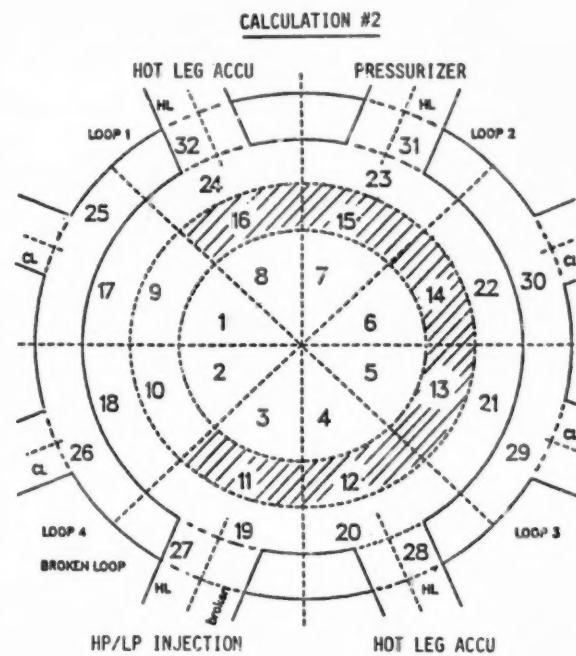
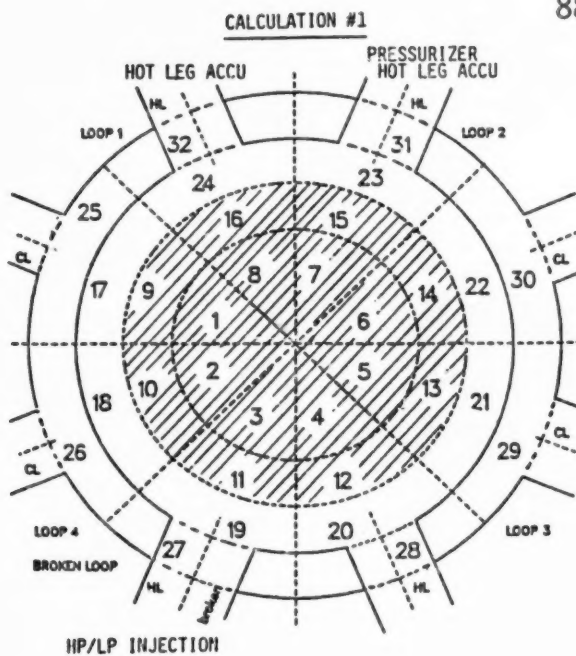
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QUENCHED CORE REGIONS (CONT.)

80 s



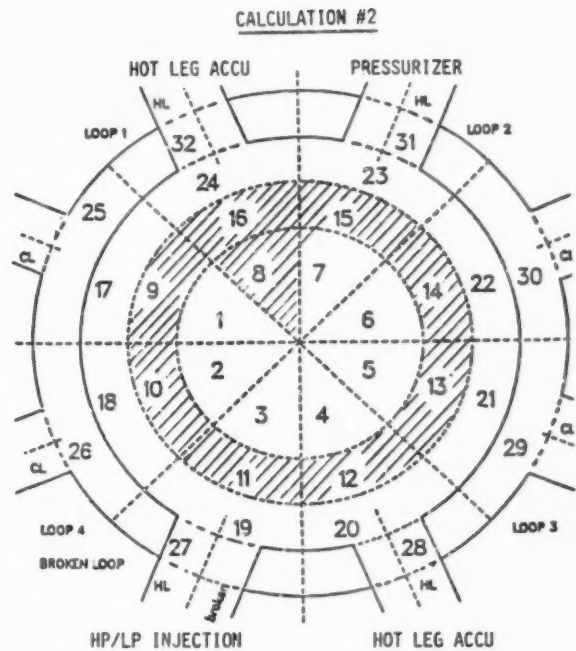
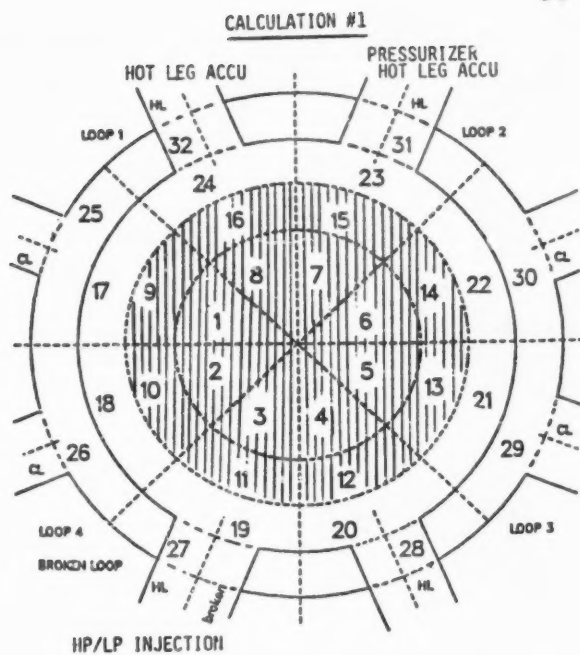
88 s



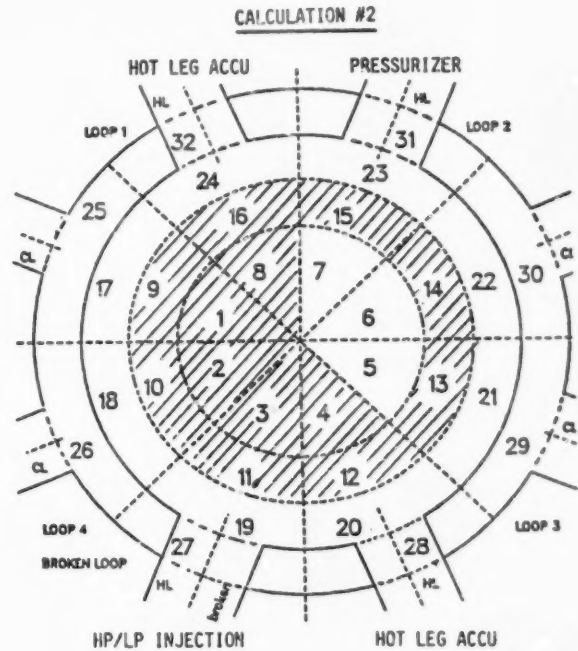
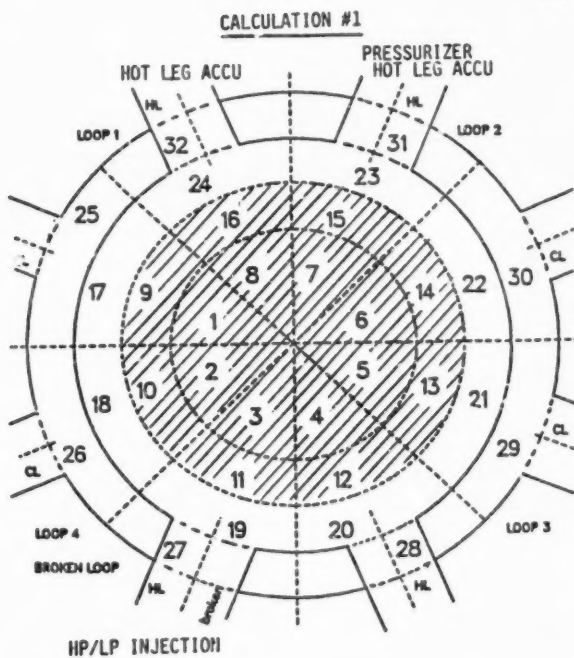
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QUENCHED CORE REGIONS (CONT.)

96 s



104 s

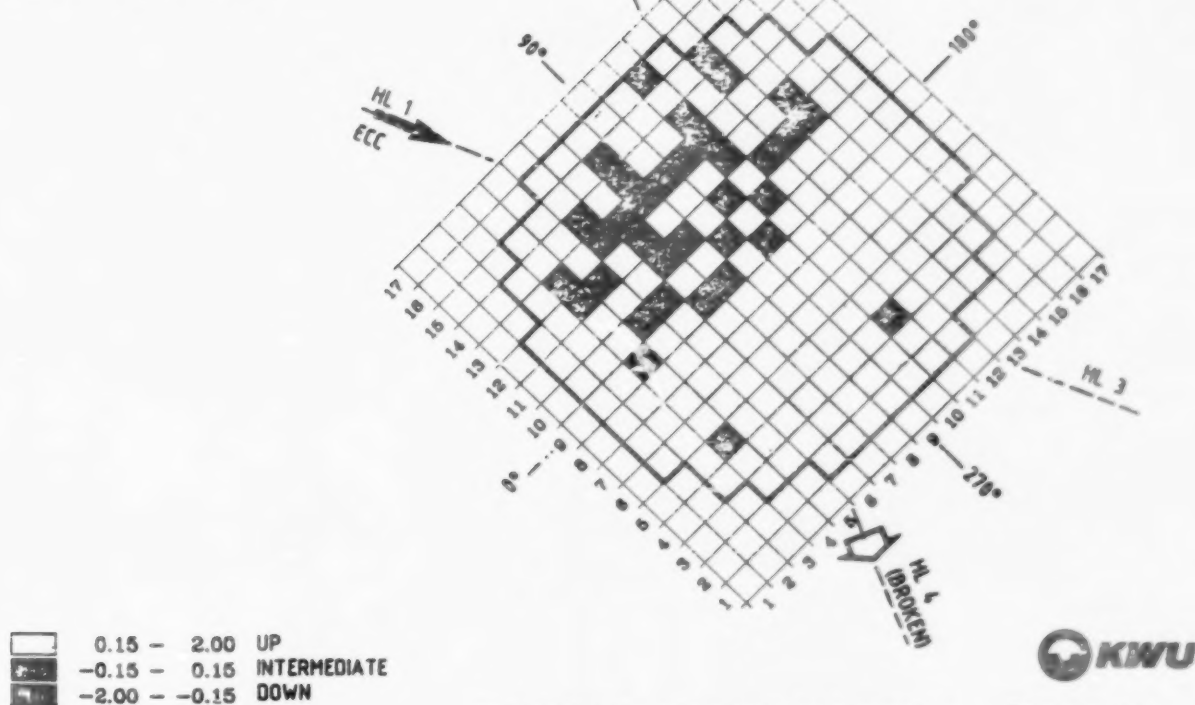


UPTF TEST RUN 013

SET TIE PLATE JKP-0 JNG-800 QHA05-30KG/S

Offset from test start: 200 sec

Data averaged over 10.00 sec



UNCORRECTED BTD FORCE IN NEWTON

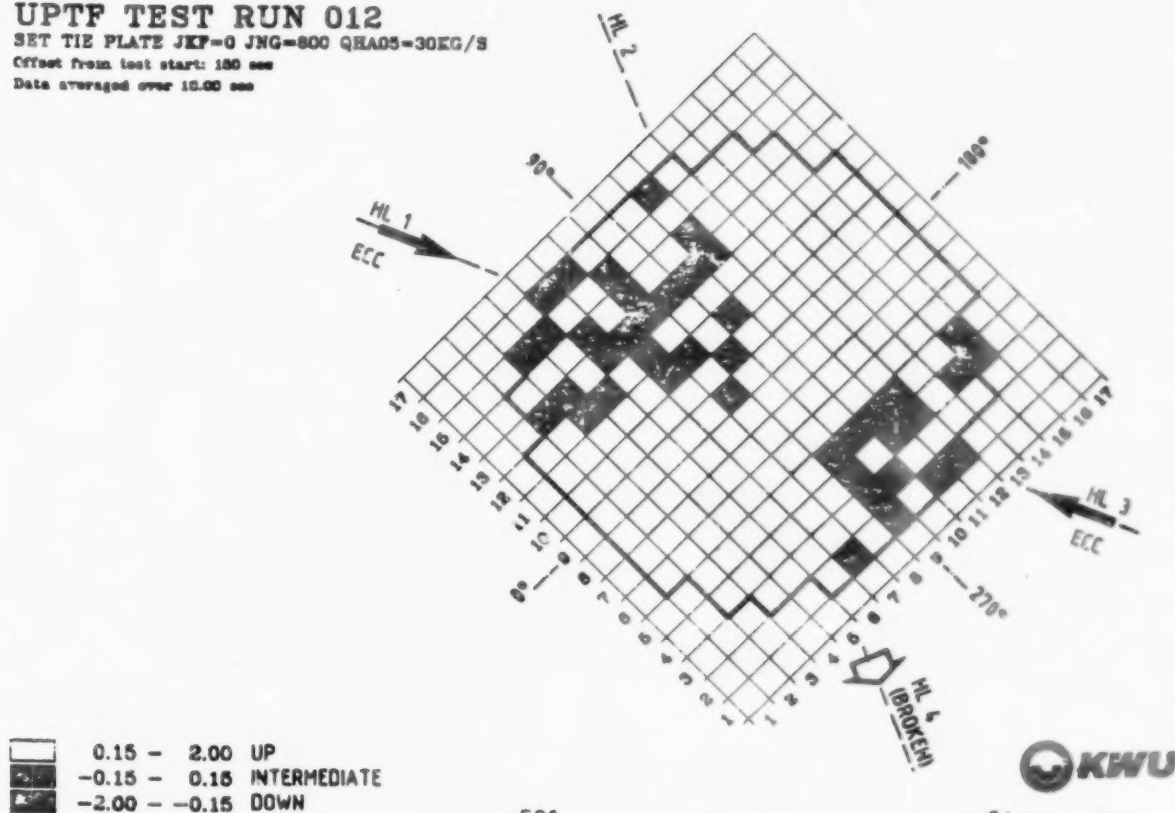
figure 16

UPTF TEST RUN 012

SET TIE PLATE JKP-0 JNG-800 QHA05-30KG/S

Offset from test start: 180 sec

Data averaged over 10.00 sec



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